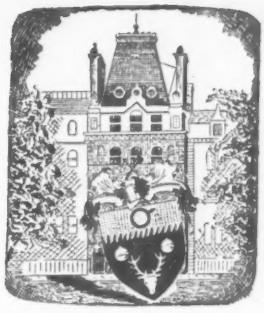
PROCEEDINGS OF THE INSTITUTE

Reports of Council · Meetings · Notices



Autumn General Meeting, 1955

THE AUTUMN GENERAL MEETING OF THE IRON AND STEEL INSTITUTE was held on Wednesday and Thursday, 16th and 17th November, 1955, at the Offices of the Institute, 4 Grosvenor Gardens, London, S.W.1. The President, Sir Charles Bruce-Gardner, BT., occupied the Chair on the first day. On the second day, in his unavoidable absence the Chair was taken by the Hon. R. G. Lyttelton, Past-President, except during the discussion of the papers by R. E. Lismer and F. B. Pickering at the morning session, when the Chair was taken by Dr. C. A. Edwards, F.R.S.

Sessions were held from 10.0 A.M. to 1.15 P.M. and from 2.30 P.M. to 4.30 P.M. on the Wednesday, and from 10.0 A.M. to 1 P.M. and from 2.30 P.M. to 4.30 P.M. on the Thursday. The Minutes of the previous Meeting, held in London on 27th and 28th April, 1955, were taken as read and confirmed.

WELCOME TO MEMBERS AND VISITORS

The **President,** Sir Charles Bruce-Gardner, Bt., offered a cordial welcome to all members and visitors attending the Meeting, and particularly to those from overseas.

OBITUARY

The **President** said that the Institute had suffered the loss of a number of members, and he mentioned by name Mr. Charles Anthony Ablett, O.B.E., who joined the Institute in 1908, and was elected an Honorary Vice-President in 1949. He had instituted the Ablett Prize to encourage the writing of papers by junior engineers. The death had also occurred of Colonel Nicolas T. Belaiew, C.B., who joined in 1912 and was awarded the Bessemer Gold Medal in 1937.

RECENT MEETINGS

The **President** referred to various meetings that had been held since the last Annual General Meeting. The outstanding meeting of last summer was the Joint Metallurgical Societies Meeting, with the American Institute of Mining and Metallurgical Engineers and the American Society for Metals. They were deeply indebted to H.R.H. the Duke of Edinburgh for agreeing to be a Patron of the British Section, which had been organized jointly by The Iron and Steel Institute and The Institute of Metals. The representatives of over 20 countries took part, and the meeting was a great success.

The Institute's Special Meeting in Scunthorpe last October was most enjoyable. They were grateful to the Lincolnshire Iron and Steel Institute, and especially to their President, Lieut.-Commander G. W. Wells, to Mrs. Wells (Chairman of the very active Ladies' Committee), and to the Mayor and Corporation of Scunthorpe and all connected with the local works.

A full report of Dr. E. C. Bain's Hatfield Memorial Lecture delivered in Sheffield University on 17th October had been given in the November, 1955 issue of the *Journal*.

The Engineers Group meetings included one for junior engineers in London from 26th to 29th September, and in connection with this visits were made to Corby and to Ford's at Dagenham. They were very much indebted to Mr. S. Fornander, Director of Research of Jernkontoret, who came specially to the meeting to deliver a lecture on " The Iron and Steel Industry in Sweden."

The **President** drew attention to the lecture on the Russian Iron and Steel Industry which was to be given that evening at 6 p.m. by Sir Robert Shone, C.B.E., and Mr. W. F. Cartwright at Seymour Hall. Sir Robert and Mr. Cartwright were members of a party which had recently visited Russian iron and steelworks.

MEETINGS IN 1956

The **President** said that the dates fixed for the Annual General Meeting next year were 16th and 17th May.

Mr. James Mitchell, C.B.E., Past President, had very kindly agreed to give a lecture to commemorate the centenary of the operation of the Bessemer process, which would fall in 1956. The lecture would probably be held on the evening before the Annual General Meeting opened (15th May).

It was also expected that the Ninth Hatfield Memorial Lecture would be given during that meeting, probably during the opening session on 16th May. Professor N. F. Mott, F.R.S., Cavendish Professor of Experimental Physics in the University of Cambridge, had kindly agreed to give the lecture, and details would be made known later.

The Council had accepted an invitation from the Chambre Syndicale de la Sidérurgie Française and the Société Française de Métallurgie to hold a meeting in France in 1956. The main part of the meeting would be from Tuesday, 5th, to Sunday, 10th June, in Paris. During that period visits would be paid to works and laboratories in the neighbourhood and also to the works at Caen. During the following week there would be a choice of three excursions: one to the works in North-East France, one to Lorraine, and a third to St. Etienne and central France. These would last about three to four days, and he was sure that they would be most enjoyable.

The Autumn General Meeting would be held in London on Wednesday and Thursday, 21st and 22nd November, 1956.

MEETINGS IN FRANCE AND GERMANY

The **President** said that the Institute had been represented at the Annual Meetings of the Société Française de Métallurgie and Verein deutscher Eisenhüttenleute, which had been held in Paris and Dusseldorf in the autumn.

In this connection, he wished to congratulate the Secretary on his election as an Honorary Member of the Verein deutscher Eisenhüttenleute. This was a rare distinction; there had been less than 30 Honorary Members since the Verein was founded, including only two others of British nationality. This was an expression of the importance which the German Institute, as well as our own, attached to the collaboration which had been developed in recent years.

NOMINATION OF PRESIDENT

The **President** announced that the Council had nominated Dr. H. H. Burton, C.B.E., Vice-President, for election as President to take office at the Annual General Meeting in 1956 to serve for one year.

Dr. Burton was a distinguished scientist who had carried out much important research in the industry and had done great work for the Institute. He was elected a member of The Iron and Steel Institute in 1935, a member of Council in 1944, and a Vice-President in 1947. Mr. James Mitchell, C.B.E., seconded the proposal.

ANNOUNCEMENTS

The **Secretary** (Mr. K. Headlam-Morley) reported the following changes on the Council since the last General Meeting in April, 1955:

Vice-President—Mr. W. F. Cartwright.

Member of Council—Mr. W. C. Bell.

Hon. Members of Council (during their periods of office)

Mr. H. H. Utley, President of the Cleveland Institution of Engineers, to succeed Mr. H. M. Morgan.

Mr. J. F. Smith, President of the Ebbw Vale Metallurgical Society, to succeed Mr. J. J. Beese.

Mr. C. J. Bushrod, President of the Manchester Metallurgical Society, to succeed Mr. J. D. Hannah.

Mr. R. L. Willott, Chairman of the North Wales Metallurgical Society, to succeed Mr. E. Taylor-Austin.

Mr. R. W. Evans, President of the Swansea and District Metallurgical Society, to succeed Professor H. O'Neill.

Retiring Members of Council

The following were due to retire at the next Annual General Meeting and were eligible for re-election:

Vice-Presidents—Mr. G. Steel, Mr. I. F. L. Elliot, and Mr. N. H. Rollason.

Members of Council—Mr. G. H. Johnson, Mr. D. A. Oliver, Dr. C. Sykes, Mr. C. H. T. Williams, and Mr. H. Boot.

Andrew Carnegie Scholarships

The following awards had been made since the last General Meeting:

£200 (2nd Grant) to **A. S. Appleton** (Liverpool University). To assist research on "The Kinetics of First Stage Graphitization in High Carbon Fe–C and Fe–Co–C Alloys, together with subsidiary Diffusion Experiments on Fe–C–Si and the above Alloys."

£410 (2nd Grant) to **C. J. Ball** (Cambridge University). To assist research on "Plastic Deformation of Metals."

£400 to **J. W. Barton** (Liverpool University). To assist research on "The Use of Paraffin Wax to Simulate Strain Patterns during Plastic Deformation of Metals."

£500 to **L. Stibe** (Cambridge University). To assist research on "Form Changes in Solid Phases."

£400 to **G. Sumner** (Manchester University). To assist research on "An Investigation of the Amplitude-Dependent Damping Capacity of Mild Steel."

ELECTION OF MEMBERS

Mr. **R. E. S. Fisher** (Sheffield) and Dr. **J. Pearson** (London), who had been appointed scrutineers of the ballot, reported that the following had been elected:

Members

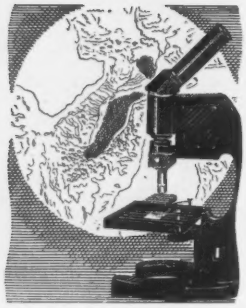
Anastassiou, Basil D. (Greece); **Azevedo,** Renato F. R. de (Brazil); **Baker,** John Roger, B.A., A.M.I.Mech.E., A.M.I.E.E. (Bellshill); **Barnes,** Richard B., B.Sc. (Manchester); **Baxter,** Deric J., B.Sc., A.M.Inst.C.E. (Scunthorpe); **Baxter,** Maurice Raymond (London); **Benedict,** Horace Arthur, M.E. (London); **Beverley,** Harry (Scunthorpe); **Bills,** Patricia Mary, B.Sc., A.R.I.C. (London); **Bird,** Philip Lorraine, A.R.I.C. (Dorking); **Boardman,** John Maginnis (Scunthorpe); **Bommen,** Bror Wessel, M.Sc. (Norway); **Borione,** R. (France); **Boulanger,** Christian (France); **Bowskill,** Harold F. (Scunthorpe); **Box,** Derrick Stanley, B.Sc., A.Inst.P., L.I.M. (London); **Bretherton,** Robert Glover, L.I.M. (Leyland); **Brookes,** Raymond Percival (Bromsgrove); **Brooks,** Anthony Sydney Morgan, B.Sc.(Eng.)Met.(Hons.) (Rotherham); **Brown,** William John (London); **Brumpton,** George Edward (Scunthorpe); **Burgon,** John (Sheffield); **Burnett,** Alan, A.I.M. (Bradford); **Carstairs,** Lindsay Steven (Luton); **Cave,** William Leslie, M.A. (London); **Chambers,** J. A. (Middlesbrough); **Chambers,** John Dawson (Scunthorpe); **Cohen,** Comdr. Kenneth, C.B., C.M.G., R.N.(Ret.) (London); **Collins,** James A., M.S.(Met.Eng.), Ph.D. (U.S.A.); **Cordero,** Raymond Philip (London); **Crittall,** John Francis, M.A., J.P. (Braintree); **Culpin,** Thomas Edward (Scunthorpe); **Da Silva,** José Madeira, (France); **Davidson,** George Anthony, A.M.C.T., A.I.M. (Dagenham); **Dearden,** Alan, A.M.C.T., A.I.M. (Bolton); **Devereux,** Alan V, B.Sc.(Hons.), A.M.I.Mech.E. (London);

Dote, Akira (Japan); **Dover,** Thomas Michael, A.I.M.M., A.C.S.M. (Scunthorpe); **Eborall,** Edward Alfred, M.I.E.E. (London); **Eccles,** David Gerald (Scunthorpe); **Edwards,** Charles Edward (Scunthorpe); **El-Gawly,** Fikry, Dipl.Ing., B.Sc. (Egypt); **Fountain,** Richard W., M.S., Ph.D. (U.S.A.); **Gamage,** Joseph Edmund, A.M.I.E.E. (Weybridge); **Giacomazzi,** Alceste, Dr.Ing. (Italy); **Gould,** Leo J., B.S.(Mech.Eng.) (U.S.A.); **Grant,** David Smith (Scunthorpe); **Gray,** Peter Robert (Scunthorpe); **Green,** E. F., M.S. (U.S.A.); **Griffith,** H. M., B.S., E.E., (Canada); **Gripenberg,** Nils Odert Leonard, Dipl.Ing. (Finland); **Guest,** Joseph C., B.Sc. (Birmingham); **Guttridge,** Joseph Cyril, G.I.Mech.E. (London); **Harper,** Alan George, L.I.M. (Oldbury); **Haslam,** Norman, M.A. (Scunthorpe); **Hayes,** Frederick Christopher (London); **Hind,** Alan, B.Eng.(Hons.Met), Ph.D., G.I.Mech.E. (Sheffield); **Holloway,** Douglas Norman (Scunthorpe); **Hook,** Ernest Clive (Scunthorpe); **Hooper,** Robert Douglas (Cardiff); **Ishihara,** Shigetoshi (Germany); **James,** Wyndham G. (Woking); **Jansen,** François Petrus (S.W. Africa); **Jenni,** Clyde B., M.S.Ch.E. (U.S.A.); **Johnson,** Robert Frederick, B.Sc. (Hons.Met.), A.I.M. (Rotherham); **Kemp,** Idris Miles (Scunthorpe); **Knapp,** Werner, Dr.Ing. (Germany); **Knibbs,** Walter John Alfred, A.C.G.I., A.M.Inst.C.E. (Stamford); **Lady,** Edward Russell, B.M.E., S.M. (London); **Lathwood,** Frederick Colin, C.G.L.I.(Met.) (Lincoln); **Le Mare,** Geoffrey, B.A. (Darlington); **Limb,** Harold Roscoe, B.Sc. (Australia); **Lingwood,** Nevil Lawrence Garrod, A.M.I.Mech.E., M.Inst.W. (London); **Lloyd,** Lt. Col. T. P., T.D., B.Sc., A.I.M. (Scunthorpe); **Lyon,** Arthur (Scunthorpe); **Lyon,** Leslie Westbrook (Scunthorpe); **Marchal,** Pierre (France); **Mathur,** Indra Narain, B.Sc.(Met.) (W. Germany); **Mellingen,** Carl (Norway); **Michard,** Jean (France); **Morea,** André A. L. (Belgium); **Morley,** K. (Scunthorpe); **Morris,** Percy, M.I.Mech.E., M.I.Loco.E. (London); **Murison,** Rae, B.Sc., A.M.I.E.E. (Scunthorpe); **Mutch,** George Harper (London); **Nair,** N. R. C., B.Sc.(Eng.) (India); **Nijhawan,** Bal Raj, B.Sc.(Met.), Ph.D., F.I.M. (India); **Oldfield,** Edwin (Birmingham); **Olette,** Michel (France); **Pacey,** Robert, A.Inst.M.E. (Scunthorpe); **Pattison,** John Robert, B.Sc., Ph.D., D.I.C., A.Inst.P. (London); **Peirano,** Manuel Angel (Argentine); **Pinder,** Robert Herbert, A.Met. (Rotherham); **Prado,** Carlos Alberto (Colombia); **Prasad,** Baidya Nath, M.Sc.(Chem.) (Newcastle-on-Tyne); **Price,** Dennis (Cardiff); **Pritchard,** John Stanger (Wolverhampton); **Raubenheimer,** James Standish (Sweden); **Raybould,** Eric, F.I.M. (Wolverhampton); **Rist,** André, Sc.D., M.S. (France); **Riviere,** Michel (France); **Robinson,** Ottiwell Oldham (Leeds); **Rodger,** William Barr, G.I.Mech.E. (Corby); **Rossington,** Donald Vernon (Sheffield); **Saunders,** Harold, A.I.Struct.E. (Scunthorpe); **Saxon,** Rodney Thomas, A.I.M. (Canada); **Schinn,** Rudolf, Dipl.Ing. (Germany); **Seatter,** Thomas George (London); **Silman,** H., B.Sc.(Hons.), F.R.I.C., M.I.Chem.E., F.I.M. (Birmingham); **Slack,** Leonard, M.Inst. B.E. (Pontypridd); **Sloot,** Frederick (Birmingham); **Smee,** Frederick William (Woking); **Smith,** Bernard Owen, M.Sc., A.Inst.P. (London); **Smith,** Douglas Allenby, A.M.I.Mech.E., M.I.Prod.E. (Birmingham); **Smith,** Eric John Charles, A.Met., A.I.M. (Chelmsford); **Smith,** W. E. (Scunthorpe); **Sorbara,** Carlo, Doct.Eng. (Italy); **Speirs,** William (Scunthorpe); **Spies,** Friedrich W. E., Met.Eng., Hon.M.Inst.B.F. (Holland); **Sutton,** Alan Lee, B.A. (Aylesbury); **Sykes,** Philip Howard, M.Sc., Ph.D., F.R.I.C. (London); **Taylor,** David William (Corby); **Teanby,** Francis Bernard, B.Sc. (Hons.Met.) (Scunthorpe); **Thomas,** Cedric (Scunthorpe); **Trentini,** Bernard, M.Sc. (France); **Twyford,** Harold, B.Sc. Tech., A.M.C.T. (Bolton); **Van Stein Callenfels,** G. W. (Holland); **Wahl,** Lucien (France); **Weir,** Charles Douglas, B.Sc., Ph.D., A.I.M. (Glasgow); **Weisz,** Michel, Dr.Ing. (France); **Wennevold,** Halfdan Th. (Norway); **White,** Frank Bernard, M.I.Prod.E. (Birmingham); **Wicks,** S. Dennis (Barnetby); **Williams,** E. L. Clive, A.M.I.Mech.E. (Rotherham); **Wilson,** William Taylor, (Scunthorpe); **Wilton,** J. W. Jr., (U.S.A.); **Winocour,** Henry (Scunthorpe); **Woolhouse,** Kenneth, A.Met., L.I.M. (Rotherham); **Zeiler,** Daniel (Scunthorpe).

Associate Members

Best, Clive Henry, Stud.I.Mech.E. (Middlesbrough); **Brown,** Arthur (Newcastle-on-Tyne); **Brummitt,** Roger Norman, B.A. (Cambridge); **Chetter,** Eric P. (Cheltenham); **El-Ansari,** Rafik Sahib (Swansea); **Fairman,** Lawrence (Newcastle-on-Tyne); **Fellows,** William (Bilston); **Fisk,** Bernard Godfrey, B.Sc., A.R.S.M. (London); **Gilbert,** Colin, B.Sc. (Blaina), **Haglington,** James Barrie (Rotherham); **Hamilton,** Collins Franklin, B.Sc.(Met.Eng.) (U.S.A.); **Harris,** Robert John (Swansea); **Hill,** G. J. B.Sc. (Luton); **Hine,** Alan John (Cambridge); **Hollingsworth,** Anthony (Sheffield); **Jones,** Michael M. (Cambridge); **May,** Walter Maxwell (Newcastle-on-Tyne); **Meredith,** James Alistair (Middlesbrough); **Middleton,** John (Sheffield); **Moore,** Colin (Sheffield); **O'Hanlon,** John Kemp (Middlesbrough); **Oxley,** Brian William (Cambridge); **Priest,** Peter Wilfred (Sheffield); **Robinson,** Peter Mather, B.Sc. (Cardiff); **Sen,** D. R., B.Sc. (W. Germany); **Stevenson,** Joseph Aidan, B.Sc. (Royal Navy); **Swann,** Peter Roland (Swansea); **Thompson,** Noel Brentnall Watson (Cambridge); **Wild,** Frank (Sheffield); **Willetts,** Denis James (Sheffield); **Williams,** Alwyn, B.Sc. (Cardiff); **Wood,** Arthur H. (Bilston); **Young,** John Marsden (Swansea).

The **President** declared these candidates to be duly elected. They numbered 172 and the total membership of the Institute was now 5005. This was the first time that the membership had exceeded 5000.



PAPERS AND REPORTS ON RESEARCH AND PRACTICE

Metallurgy · Plant Operation · Research

The Stress-Corrosion Cracking of Austenitic Stainless Steels

Part I—MECHANISM OF THE PROCESS IN HOT MAGNESIUM-CHLORIDE SOLUTIONS

By T. P. Hoar and J. G. Hines

SYNOPSIS

Austenitic stainless-steel wires of several usual compositions based on 18% chromium–8% nickel, stressed in tension, have been exposed to 42% aqueous magnesium-chloride solution at 135–154° C in apparatus allowing measurement of the corrosion potential and of the extension of the exposed specimens during test.

Potential/time curves obtained with unstressed specimens show a rise to a maximum followed by a fall, and are generally similar to those obtained on tin and on mild steel under conditions where oxide-film repair is followed by film breakdown and pitting. Stressed specimens behave similarly in the early stages of exposure, the potential first rising steadily; it then falls relatively rapidly during the last few minutes before fracture of the wire. Extension measurements during tests, and the mechanical properties of specimens removed before fracture, show that crack propagation takes place only during the final rapid potential fall, and thus usually occupies only a small part of the total life of a specimen. At high applied stresses, cracking begins before oxide-film repair as indicated by the potential rise is complete; at low stresses it does not begin until some time after film breakdown. At high applied stresses the overall process is very sensitive to temperature; the apparent activation energy for the process of the induction period before cracking is about 40 kcal/g-mol. and that for the process of crack propagation is about 10 kcal/g-mol.

At high applied stresses the times to fracture of steels of different compositions are similar and not greatly influenced by the stress value. Generally, the influence of variation of composition is indirect and much less marked than that of other factors such as mechanical and surface conditions. At low stresses the times to fracture are very sensitive to the stress value, although stress has little influence on the processes occurring during the induction period of corrosion damage preceding crack propagation, and the rate of crack propagation is substantially independent of the stress value; but more extensive corrosion damage is required before cracks can be initiated by lower stresses.

The results are discussed in terms of corrosion processes similar to those found in known cases of film repair and breakdown; of crack initiation at susceptible points on the bare metal surface; and of crack propagation by the rapid anodic dissolution of highly stressed and strained metal.

Preliminary experiments have shown that the processes occurring during the induction period, and that of crack propagation, can be greatly retarded by relatively mild cathodic protection. 1146

INTRODUCTION

THE WORK described in the present paper, part of a programme of research into the fundamental mechanism of stress corrosion, is a study of the transgranular stress-corrosion cracking of the austenitic stainless steels. A brief account of some of the work has already been published.[1]

Stress-corrosion cracking of the austenitic stainless steels is of two types: it is either essentially intergranular or transgranular. Intergranular cracking seems to be associated with carbide precipitation, and can be avoided by the use of suitable steels or by suitable heat-treatments.[2] It occurs in conditions that lead to intergranular corrosion in the absence of stress; stress seems merely to accelerate failure. Stress is, however, a necessary condition for the

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Dr. Hoar and Mr. Hines are at the Department of Metallurgy, University of Cambridge.

transgranular cracking with which the present work is concerned.

Previous Work

Environments that can cause stress-corrosion cracking in austenitic stainless steels have been studied by several previous workers.[3–6] Service failures have been reported in a variety of environments, mainly containing inorganic chlorides at elevated temperatures[3, 4, 7]; 50% sodium-hydroxide solution at 200° C,[7] impure steam,[6, 8] and moist ethyl chloride[3] have also caused failure. In the laboratory, similar cracking has been produced in boiling inorganic chloride solutions,[4–6] sodium-chloride–hydrogen-peroxide solution,[9] and sodium-chloride solution containing other oxidizing agents.[6] Rees[10] has reported cracking caused apparently by hydrogen sulphide, but Truman[11] was unable to produce typical stress-corrosion cracking by the use of hydrogen sulphide. Boiling magnesium-chloride solution, containing approximately 42% w/w $MgCl_2$, has been much used in experimental work; it will crack all usual grades of austenitic stainless steel.

Increase of applied stress reduces the time to fracture for most alloys exposed to stress-corrosion conditions; it has been suggested that in some cases a minimum stress is necessary for cracking to occur. Thus Franks, Binder, and Brown[5] found that certain annealed 18–9 and 17–7 Cr–Ni steels did not crack in boiling 42% magnesium-chloride solution at stresses below 38,500 lb/in^2 (approximately the 0·2% proof stress) and that the cold-rolled materials did not crack below about 50,000 lb/in^2. However, Scheil[4] has reported cases of cracking with applied stresses of below 10,000 lb/in^2. Quenching stresses have been sufficient to cause cracking,[3] particularly near welds of austenitic steels to ferritic steels. Edeleanu[12] and Farmery[13] have shown that presence or absence of stress in the early stages of exposure often makes little difference to the subsequent onset of stress-corrosion cracking in aluminium alloys.

Little work has been done on the influence of temperature on the overall rate of stress-corrosion cracking. Winterstain, McDonald, and Waber[14] measured the times to fracture of mild-steel wires in hot nitrate solutions at various temperatures, and deduced a value for the apparent energy of activation of the process of 10 kcal/g-mol. Edeleanu[6] has reported that the form of cracking of austenitic stainless steels in chloride solutions of low boiling point is different from that found in hot magnesium-chloride solutions.

Many workers have used measurements of the single electrode potential of a corroding metal to follow the progress of corrosion reactions, especially the repair and/or breakdown of surface oxide films. Brennert[15] found that the potential of tin exposed to chloride solutions at first rises (i.e. becomes more noble), and then falls with the onset of ' black spot ' corrosion. One of us[16] showed that the rise and fall may be explained by partial repair followed by breakdown of the oxide film covering the metal, the partial repair being effected by the plugging of the holes of the film by anodically produced hydroxide or oxide and the subsequent breakdown by the dissolution of the metal to soluble products owing to the rise of acidity in the pore; and later[17] that mild steel in chloride solutions, plain and containing also inhibiting anions, behaves in a qualitatively similar way. Edeleanu and Evans[18] have explained the onset of pitting in aluminium by similar reactions. Gilbert and Hadden[19] found that the potential of an unstressed Al–7% Mg alloy in sodium-chloride solution showed the same rise and fall; stressed specimens behaved similarly, but after cracking had begun there was a slight extra fall in the potential, which rose again after fracture of the specimen. ' Kicks ' of potential in the negative sense were observed during crack propagation. The potentials of the stressed specimens were found to be generally slightly less noble than those of the unstressed. Logan[20] found that the potentials of various ferrous and non-ferrous alloy specimens became less noble as they were subjected to a continually increasing stress, the potential reaching a minimum just above the yield point. He explains the phenomenon in terms of oxide-film breakdown under continually increasing stress, and concludes that the potential difference between filmed and unfilmed metal is an important factor in stress-corrosion cracking; the results, obtained with a non-constant stress, are not necessarily to be expected in the early stages of stress-corrosion cracking under constant stress.

Gilbert and Hadden[19] and Edeleanu[12] showed that crack propagation in Al–Mg alloys occurs in discontinuous jerks. They also found that cracking proceeds fastest in the final stages of exposure of the specimen —a fact recently confirmed by Jones[21] and by Farmery.[13]

Methods of Testing

Many investigations of stress-corrosion cracking have been carried out with bent sheet specimens of the horseshoe type.[4–6] Such test pieces are convenient, but there is an inherent difficulty in estimating the stresses present, and the stress distribution is complex. Plastic deformation producing complex residual stresses in the material, and in some cases an unspecific degree of strain-induced phase change, occurs in forming the specimens, which are then further stressed by forcing them into a jig. The indefinite, low yield point of the 18–8 class steels makes it almost impossible to stress specimens uniformly and reproducibly in the elastic range.

Variations in stress from grain to grain, and the stress-concentration effect once pitting or cracking has begun, make it impossible to obtain uniform stress and to avoid plastic deformation completely in any type of specimen, but it is desirable that a testing apparatus should allow, as far as possible, a reasonably uniform reproducible stressing in the elastic or plastic ranges of the material of the specimen. Cylindrical specimens loaded in tension have a fairly simple stress pattern, and the symmetry of such specimens is also desirable in assisting uniformity of corrosion conditions. For these reasons, and because their use allows the use of small corrosion and mechanical apparatus, wire test specimens direct-loaded in tension were adopted for the present

work; Jones[21] has found them convenient in work on Al–Mg alloys.

An important difference between tests on bent specimens and those on specimens under constant direct load in tension is that, whereas in the former case yielding ahead of a crack tends to relieve the stress causing it, in the latter cracking increases the stress by decreasing the remaining cross-section of the specimen. Specimens tested under constant direct load thus fracture completely more rapidly than those tested in a bend test. In many practical cases of stress-corrosion cracking, quite small cracks near welds, rivets, or bolts may cause leaking and necessitate replacement of plant. Such cracking may well relieve the stresses that have caused it and may then cease, but its early stages are in practice decisive. The direct-load test, especially with specimens of small cross-section, minimizes the period of crack propagation to produce fracture, and may thus be a more reliable guide to the relative suitability of different steels for practice than are tests where stress relief occurs. In these, small cracks may be present for a long time before they are noticed, while in a direct-load test with specimens of small cross-section they are rapidly detected by the relatively quick complete fracture.

Hot concentrated aqueous magnesium chloride was adopted as the basic corrodent in the present work, because (*a*) it causes cracking of all austenitic steels under stress and (*b*) it gives rapid and reasonably reproducible results. We do not believe that results with different materials in hot aqueous magnesium chloride can be used directly to assess the relative general susceptibility of the materials to stress-corrosion cracking—the conditions provide neither a true accelerated test nor a test of an inherent property of the materials. Rather, they provide a convenient experimental means whereby the mechanism of *one* case of stress-corrosion cracking can be studied in detail within a reasonable time, which was the object of the work now reported.

EXPERIMENTAL TECHNIQUE

Apparatus

The testing unit allows tests on wires of diameters of the order of 0·02 in. stressed in tension and immersed in cold or hot solutions (Fig. 1). The specimen passes concentrically through the unit, being fixed to an adjustable grip at the lower end and direct-loaded by means of a weight-carrying lever at the upper end. The corrodent is contained

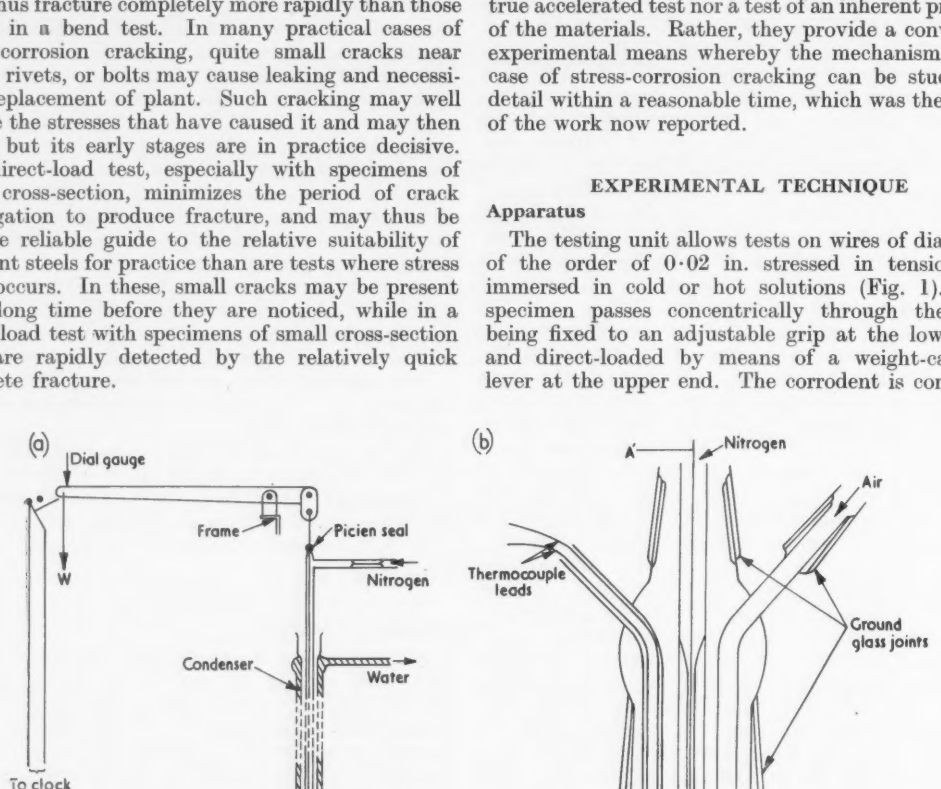


Fig. 1—(*a*) Single unit of testing battery; (*b*) detail of (*a*)

in a Pyrex glass tube, constricted at its lower end. A Nichrome heating coil is wound directly on to the outside of the tube, and protected by a thin film of baked shellac. The solution below the heating coil remains cool, and in the case of 42% magnesium-chloride solution (extensively used in the work) solidifies in the constriction, which is also sealed with Picien wax. A water condenser is fitted by a ground-glass joint to the upper end of the tube, and three Pyrex tubes lead into the solution. One of these is sealed off, and contains a thermocouple hot-junction; the second is drawn down to a capillary and allows air* or other gas to be bubbled through the solution, while the third is left open and allows electrodes to be inserted for electrochemical measurements. Five units are built on a single frame.

Experiments with a prototype unit showed a tendency for fracture of the wire specimen to occur at, or above, the liquid level. This was overcome by surrounding the upper part of the specimen with a Pyrex tube, dipping just below the surface of the solution; purified nitrogen is passed down the tube and allowed to bubble out at the lower end. This prevents all attack at or above the liquid-line. The gas supply to the nitrogen sheaths in the several units is purified in a single train, the supply to an individual unit passing through a length of capillary tube before reaching the sheath; the capillary causes a large pressure drop so that small variations in the resistance to gas flow in an individual unit produce negligible effects on the rate of flow in that unit or in the others. The supply to the aeration jets passes through similar capillaries; in both cases the capillaries are so adjusted that the flow of gas in the individual units is the same to $\pm$ 1%.

The temperatures of the solutions are measured thermo-electrically by copper/constantan thermo-couples in series with a millivoltmeter. The heating coils are supplied from d.c. mains, the power input of each unit being controlled by a series resistance. Below the boiling point the temperatures of the solutions can be held within $\pm$ 2° C, and at the boiling point more accurately, by these means.

The load-carrying levers in falling switch off electric clocks and thus register the instant of fracture of the wires.

The variation of the potential of the specimen while exposed to stress-corrosion conditions was followed with the apparatus modified as shown in Fig. 2. A string wetted with magnesium-chloride solution leads from a side tube below the heating coil on the main tube containing the solution. The string does not reach the hot solution, being held in a solidified plug of magnesium chloride that seals the side tube. The string leads to a pot containing cold N magnesium-chloride solution, from which further liquid connexion to the N sodium chloride of a silver/silver-chloride reference half-cell is made. Paraffin wax is used where indicated to avoid spurious effects caused by stray currents from the heating coil carried by an aqueous film caused by the deliquescent magnesium chloride. The potential difference between the reference half-cell and the specimen is measured by an electrometer; a Marconi battery-type pH meter was used.

The system described gives steady potential readings, reproducible within a run and between runs to within a few millivolts. The potential differences measured are those of the cell:

Specimen	42% $MgCl_2$ (aq.)	Sat. $MgCl_2$ (aq.)		
	150° C	150–20° C		
N $MgCl_2$ (aq.)	N NaCl (aq.)			
20° C	Sat. AgCl		Ag	

Because of the uncertain junction potentials, and of temperature and concentration effects, the potential of the specimen on the hydrogen scale cannot be accurately determined. However, these factors are not likely to change appreciably, and certainly not suddenly, once the apparatus has reached thermal equilibrium. The potential values reported have been obtained by adding 250 mV to the measured potential differences (Ag/AgCl/NKCl; $E_{h,\,25^\circ C} = +\,0{\cdot}238$ V),

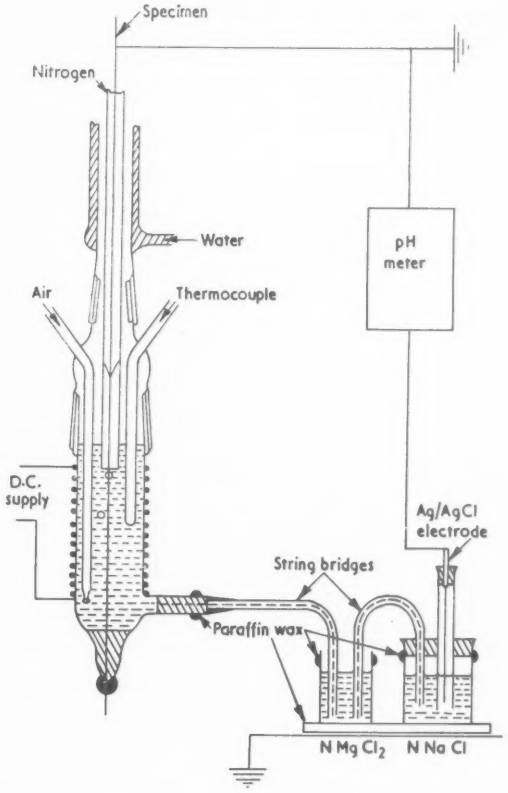


Fig. 2—Modification of apparatus for measurements of corrosion potential

* It was found that when air was passed through the capillaries the times to fracture of specimens under the present experimental conditions were not significantly different from those obtained when no air was intentionally admitted or when argon was passed. In the main experiments a constant slow stream of air (200 ml (N.T.P.) per h) was passed to provide constant conditions.

Table I

CHEMICAL ANALYSES AND MECHANICAL PROPERTIES OF STEELS

Steel	Analysis, wt.-%										Mechanical Properties		
	C	Cr	Ni	Si	Nb and Ta	Mo	Mn	Cu	Ti	N	0·1% Proof Stress, lb/in²	U.T.S., lb/in²	Elongation, %
18–8(*a*)	0·11	19·1	8·5	0·47	N.D.	0·45	0·78	N.D.	N.D.	N.D.	52,300	117,800	46·8
18–8(*b*)	0·09	17·1	9·2	1·0	<0·05	0·18	0·78	0·2	<0·05	0·06	51,600	111,700	65·8
18–8–LC(*a*)	0·08	19·0	9·9	0·56	N.D.	0·15	0·65	N.D.	N.D.	N.D.	52,600	108,400	54·0
18–8–LC(*b*)	0·07	19·2	9·8	0·45	<0·05	0·05	0·84	0·1	<0·05	0·04	44,000	114,200	65·7
18–8–Ti(*a*)	0·11	19·5	7·8	0·50	N.D.	0·23	0·38	N.D.	0·7	N.D.	70,000	119,300	28·5
18–8–Ti(*b*)	0·07	18·8	8·1	0·43	<0·05	0·13	0·65	0·4	0·16	0·03	51,500	119,400	60·6
18–10–Nb	0·08	19·3	9·3	0·80	1·0	0·14	0·72	0·1	0·06	0·02	48,400	114,400	51·5
18–8–3Mo(*a*)	0·09	18·4	8·2	0·47	N.D.	2·6	0·60	N.D.	N.D.	N.D.	72,050	136,800	44·6
18–8–3Mo(*b*)	0·07	18·7	8·0	0·32	<0·05	3·0	0·36	0·3	<0·05	0·06	72,300	138,000	43·6
18–10–3Mo–Ti	0·07	16·9	10·0	0·51	<0·05	2·5	1·6	0·2	0·33	<0·005	52,000	120,650	54·0

N.D. = Not determined

and are thus an approximation to the true values on the hydrogen scale.

Materials

The steel wire, 0·020 in. (0·051 cm) in diameter, used in the investigation was supplied by the Stainless Steel Wire Company of Sheffield. Two batches of material were used; the first comprised four different compositions, simple 18–8* chromium–nickel, 18–8 low-carbon, 18–8 titanium-stabilized, and 18–8–3 molybdenum; and the second, in addition to further supplies of these types, 18–10 niobium-stabilized and 18–10–3 molybdenum titanium-stabilized. These compositions are referred to below as 18–8, 18–8–LC, 18–8–Ti, 18–8–3Mo, 18–10–Nb, and 18–10–3Mo–Ti; the two batches are distinguished by the addition of (*a*) or (*b*).

Chemical analyses of the steels determined by standard methods, and mechanical properties determined by the Hounsfield Tensometer, are listed in Table I. The analyses of certain of the steels show them to have been outside the usual specifications. Steel 18–8–Ti(*b*) contained considerably less titanium than is necessary to stabilize its carbon, and both this steel and steel 18–8–3Mo(*b*) contained relatively large quantities of copper. Steel 18–10–3Mo–Ti contained less chromium than usual (a result compatible with its low ferrite content, found metallographically). The mechanical properties of the steels show that, although they were bright-annealed after the final wire-drawing pass, they had undergone some subsequent work-hardening, probably during reeling and unreeling. Simple magnetic tests with a hand magnet showed that the three steels 18–8–3Mo(*a*) and (*b*) and 18–8–Ti(*a*) were strongly ferromagnetic, and that steel 18–10–3Mo–Ti was slightly ferromagnetic. The strong magnetic reaction of steel 18–8–Ti(*a*) and its high 0·1% proof stress indicate a considerable degree of cold work in the material.

The wires showed usual microstructures in accordance with their various compositions; some are illustrated later in photomicrographs of cracked and fractured specimens.

The experiments described in this paper were mainly carried out on the steels 'as-received'—bright-annealed, water-quenched, and wound on reels after the final drawing pass. The surfaces were clean and bright; they were degreased by swabbing successively with ethanol (or carbon tetrachloride) and acetone.

The basic corrosive environment was aqueous magnesium-chloride solution containing about 42% w/w $MgCl_2$ and boiling at 154° C. The solutions were made up from approximately 1375 g of the hexahydrate 'pure crystals' and 50 ml of water, salt or water being added until the correct boiling point was obtained. The rapid variation of the boiling point with concentration in this region makes this method sufficiently sensitive: no variation of time to fracture ascribable to variations of concentration has been found. In experiments where additions were made to the basic solution, they were of 'Analar' grade chemicals. A few experiments were made with 'Analar' magnesium chloride for the basic solution.

RESULTS AND INTERPRETATION

The experiments comprised four main groups:

(i) Preliminary tests of the capabilities of the apparatus and measurements of the times to fracture by stress-corrosion cracking of various grades of stainless steels in hot concentrated magnesium-chloride solution

(ii) Measurements of the variation of the corrosion potential of stainless-steel specimens exposed to stress-corrosion conditions and the correlation of this potential with the progress of stress-corrosion cracking as shown by mechanical tests

(iii) The influence of variations in the experimental conditions on the resistance of the steels to stress-corrosion cracking, on the potential/time curves, and on the nature of the cracking

(iv) Metallographic examinations of cracked specimens.

Preliminary Experiments

It was found that the apparatus gave reasonable reproducibility between the individual times to fracture within a run (five specimens tested simultaneously under similar conditions) and between separate runs carried out under similar conditions. The standard deviation of the times to fracture within a run was of the order of 20% of the mean in the early experiments; smaller scatters have been

* The figures indicate weight percentages.

obtained in later experiments on some steels, but it seems unlikely that the scatter can be appreciably reduced by further refinement of the experimental conditions.

Examination of specimens after failure showed that the steels can be divided into two classes according to the number of cracks produced. Steels 18–8(*a*) and (*b*), 18–8–LC(*a*) and (*b*), 18–8–Ti(*a*), 18–8–Nb, and 18–10–3Mo–Ti usually showed one major crack which produced fracture, with a few minor cracks at other parts of the specimen. These cracks were more numerous near the point of failure than in other parts of the specimen; the cracking found in steels 18–10–Nb and 18–8–LC(*b*) was confined almost entirely to a region less than 20 mils on either side of the fracture. The other three steels, 18–8–3Mo(*a*) and (*b*) and 18–8–Ti(*b*), usually showed several large cracks over the whole exposed length of the specimens, sufficient to cause them to break into short lengths on bending. These regions of severe cracking were more frequent (4 per in. of specimen) with steel 18–8–3Mo(*a*), than with steels 18–8–3Mo(*b*) (2–3 per in.) and 18–8–Ti(*b*) (<1 per in.). A few specimens of steel 18–10–3Mo–Ti showed one such major crack apart from the point of failure, while one or two specimens of steel 18–8–3Mo(*a*) showed no major crack apart from that causing failure. These specimens showed breaking times considerably longer than those of the remainder of their groups. The nature and distribution of the cracking is discussed in more detail later.

Measurement of the Corrosion Potential of Specimens Exposed to Stress Corrosion

The results described in this section were obtained with the modified apparatus shown in Fig. 2. The five specimens tested at a time were connected to each other and to the negative terminal of the pH meter: the reference electrodes were connected to the other meter terminal through a five-way switch. Successive readings of the potentials of the five specimens could be made within 30 s; the potentials were noted at intervals of 1–20 min, the interval depending on the rate of variation of the potential.

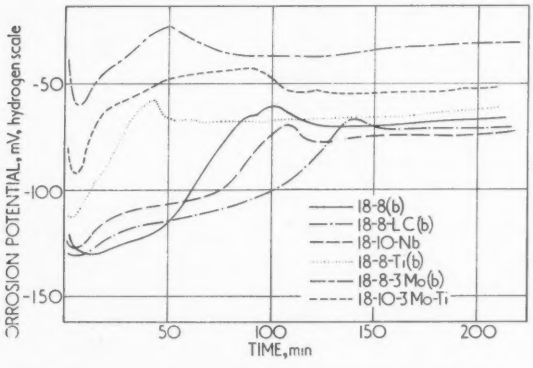


Fig. 3—Typical potential/time curves for unstressed specimens: steels 18–8(*b*), 18–8–LC(*b*), and 18–8–3Mo(*b*), 154° C; 18–8–Ti(*b*), 153° C; 18–10–Nb and 18–10–3Mo–Ti, 152° C

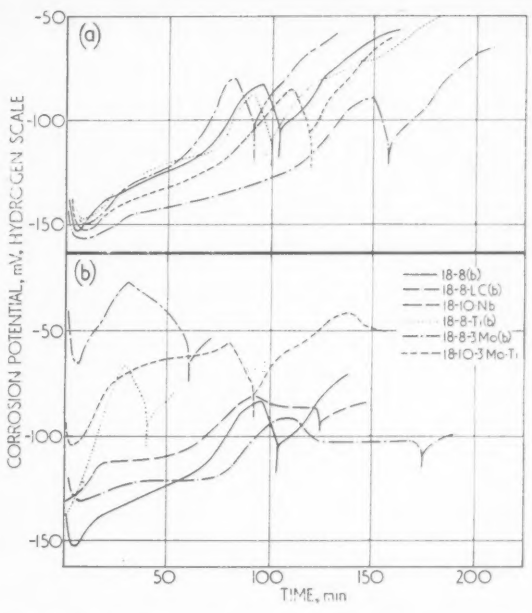


Fig. 4—Typical potential/time curves for stressed specimens: (*a*) steel 18–8(*b*), 48,000 lb/in², 153° C (quintuplicate); (*b*) steels 18–8(*b*), 18–8–LC(*b*), 18–10–Nb, and 18–8–3Mo(*b*), 48,000 lb/in²; 18–8–Ti(*b*), 32,000 lb/in²; 18–10–3Mo–Ti, 24,000 lb/in² All at 151–153° C

In a few experiments the potential of single specimens was read at intervals of 10 s to obtain details of parts of the potential/time curves.

General Form of Potential/Time Curves

Figure 3 shows typical potential/time curves given by unstressed specimens of the second batch of steels. The curves are similar in general form to those previously obtained by one of us for tin[16] and mild steel[17] in dilute salt solutions at 25° C, and may reasonably be explained in terms of a similar series of stages:

(i) The potential falls as solution penetrates into pores or cracks in the oxide film, and metal anodic reactions begin

(ii) Metal cations pass into solution at the anodic points at the base of the pores: the solubility product of the metal hydroxide or oxide at the local pH is soon exceeded, so that hydroxide or oxide is precipitated or directly formed from metal in the lattice by hydroxyl-ion discharge. The plugging of the pore, or film repair, is reflected in the rise of the corrosion potential

(iii) Precipitation of hydroxide or oxide is accompanied by a fall in pH in the pore, and eventually ceases. Unless the pore has completely healed, the metal now starts to dissolve to soluble products at its base. Film-breakdown conditions have been established: the pore grows with solution of metal and hydroxide, helped by film undermining. Thus the corrosion current increases, and the measured corrosion potential falls, while a corrosion pit forms at the site of the original pore. The corrosion rate reaches a more or less steady value and the potential becomes steady. After relatively long

times the potential may rise slightly, presumably because of choking of the reactions by corrosion products.

The curves given by the two molybdenum-bearing steels differ from those given by the others in that the potential is throughout higher, its rise during film repair is less, and its fastest rise occurs somewhat earlier. In general, all the steels give qualitatively similar but quantitatively different potential/time curves. It is likely that the oxide films on the different alloys vary in their initial state and that their rates of repair are different.

Figure 4*a* shows potential/time curves for quintuplicate stressed specimens of steel 18–8(*b*) and Fig. 4*b* shows typical potential/time curves for a number of steels under various stresses. The curves are generally displaced by 10–20 mV in the negative sense from those given by corresponding unstressed specimens (Fig. 3), but are otherwise similar until the potential falls rapidly during the last few minutes before specimen fracture; this fall ends in a pronounced negative kick at the instant of fracture. The scatter of the times to fracture shown in Fig. 4*a* is evidently caused almost entirely by differences in the period before the rapid potential fall.

The rapid potential fall may start either before or after general film-breakdown conditions have been established, as shown in Fig. 4*b* by, for example, the curves for steels 18–8(*b*) and 18–8–LC(*b*) respectively; for a given steel it occurs earlier the higher the applied stress, as shown in detail later. After specimen fracture, the potential behaves similarly to that of an unstressed specimen.

The duration and extent of the rapid potential fall are different for the various steels; the curves shown in Fig. 4*b* were not all obtained for equally stressed specimens, but similar variations from steel to steel were also found when the steels were tested at equal applied stress. The mean durations and extents of the potential falls are tabulated in Table II: it will be seen that the fall is more extensive, and longer, for materials showing the greater number of and more extensive cracks.

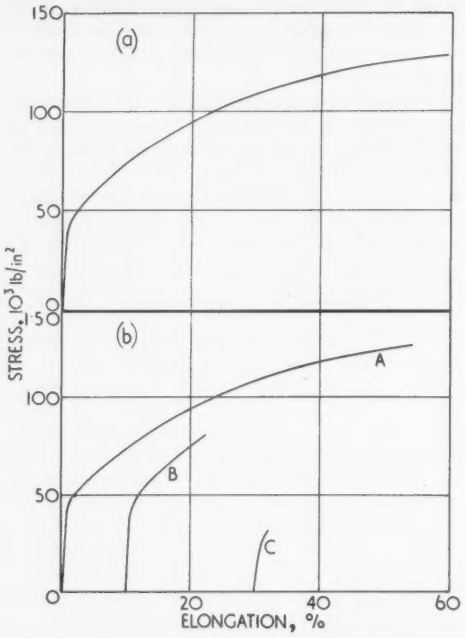


Fig. 6—Stress/elongation curves, steel 18–10–3Mo–Ti: (*a*) typical curve of control specimen exposed to stress alone; (*b*) typical curves of specimens exposed to stress and corrosion, *A* removed before, and *B* and *C* after the start of the rapid potential fall

Correlation of Potential/Time Curves and the Progress of Stress-Corrosion

Most of the data reported in this section, with the exception of some extension measurements, were obtained on steels 18–8(*b*) and 18–10–3Mo–Ti. A few confirmatory experiments were performed on the other steels.

Variation of Mechanical Properties with Time of Exposure to Stress-Corrosion Conditions—A number

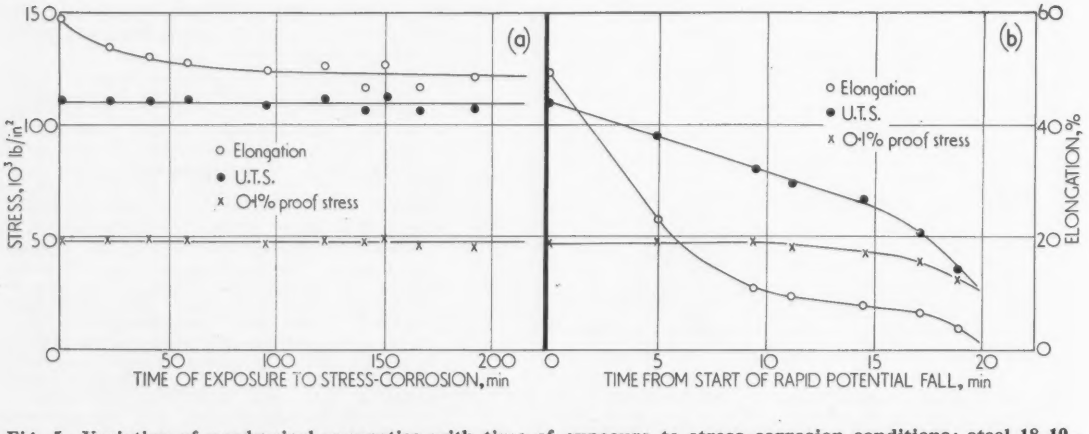


Fig. 5—Variation of mechanical properties with time of exposure to stress-corrosion conditions: steel 18–10–3Mo–Ti at 24,000 lb/in², 153° C. Specimens removed (*a*) before and (*b*) after the start of the rapid potential fall

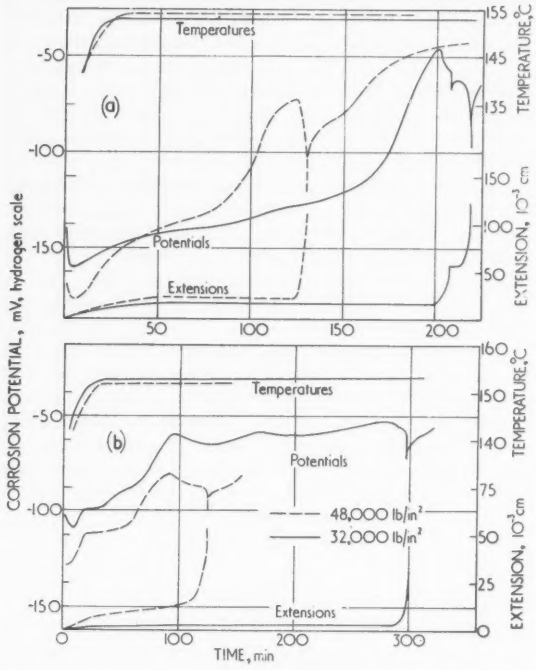


Fig. 7—Extension/time, potential/time, and temperature/time curves for (*a*) steel 18–8(*b*), 48,000 lb/in² (two specimens), and (*b*) steel 18–10–Nb, 48,000 and 32,000 lb/in²

of specimens exposed to stress-corrosion conditions were removed during the test but before fracture, rinsed in warm water, and dried with acetone; their mechanical properties were then determined with a Hounsfield Tensometer. Control specimens were taken from parts of the wire that had been exposed to stress but not to corrosion. Figures 5*a* and *b* show the variation of the 0·1% proof stress, ultimate tensile strength, and elongation of steel 18–10–3Mo–Ti with time of exposure, for specimens removed before and after the start of the rapid potential fall before fracture; the specimens had been stressed to 24,000 lb/in², in 42% magnesium-chloride solution at 153° C. It may be seen that the values of the 0·1% proof stress and tensile strength were not significantly reduced until the start of the rapid potential fall, although the elongation was reduced slightly. Once the final stage had started, the loss of strength became marked; the elongation was affected at an earlier stage than were the proof stress or tensile strength. Figure 6 shows typical stress/elongation curves obtained with the same steel; from the shape of the curves it is clear that the initial stages of cracking will be reflected most in the elongation; strain hardening, and the locally increased apparent yield stress due to the triaxial stress system, near a crack tip will prevent reductions in the proof stress from becoming apparent until the breaking stress has been reduced almost to the proof stress.

Extension of Specimens during the Stress-Corrosion Process—The extension of specimens under test was measured by registering the movement of the lever loading the specimens on a dial gauge capable of reading displacements of 0·02 mil. In these experiments the temperature of the solution was continuously noted; the solution had to be added after the apparatus had been assembled, and although it was added as close to its boiling point as possible, its temperature was lower than the boiling point for the first few minutes of a run.

Dial-gauge readings and potential measurements for two of the steels are shown in Fig. 7. The small extension in the early stages of a run was smooth, and was probably mainly caused by thermal expansion of the specimen while the solution was being heated to its boiling point, and while thermal equilibrium

Table II

CHARACTERISTICS OF POTENTIAL/TIME AND $\log_{10} t_f/S$ CURVES

The following are *mean* values, derived from numerous replicate experiments, and consequently are usually slightly different from the values shown on the corresponding *typical* curves in the diagrams

Experimental Steel	Period of Film Repair for Unstressed Specimens $t_{r(0)}$, min	Duration of Rapid Potential Fall (Period of Crack Propagation), min, at Applied Stress of			Extent of Rapid Potential Fall, mV, at Applied Stress of			Time to Fracture $t_{f\Sigma}$ at Stress Σ such that $t_{f(\Sigma)} = t_{r(0)} + t_{c(\Sigma)}$, min	t_f at Change in Gradient of $\log_{10} t_f/S$ Curve, min	Time to Fracture $t_{f(0)}$ at Zero Applied Stress, min	Type of Cracking*
		48,000 lb/in²	40,000 lb/in²	20,000 lb/in²	48,000 lb/in²	40,000 lb/in²	20,000 lb/in²				
18–8(*a*)	...	...	...	...	...	...	... ..	...	190	Very long	G
18–8(*b*)	120	6	7–8	...	20	18	...	125	160	,, ,,	G
18–8–LC(*a*)	...	...	...	...	...	...	...	...	140	,, ,,	G
18–8–LC(*b*)	135	4–5	6	...	10	8	...	140	455	,, ,,	R
18–8–Ti(*a*)	...	...	...	...	...	...	...	...	345	,, ,,	R
18–8–Ti(*b*)	41	10	...	12–14	25	...	15	45	85	250	S
18–10–Nb	140	5	6–7	...	9	8	...	145	315	Very long	R
18–8–3Mo(*a*)	...	...	...	...	...	...	...	...	...	100	V.S
18–8–3Mo(*b*)	61	20	...	24	35	...	25	85	100	300	S
18–10–3Mo–Ti	105	10	...	13	22	...	18	115	150	8000	G–S

* G: Numerous cracks distributed over most of the specimen
R: Cracks restricted to region of fracture
S: Severe complex cracking at many points

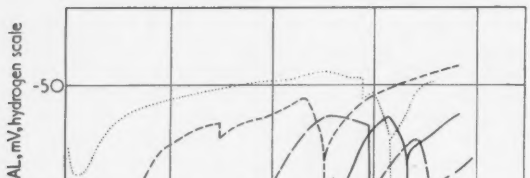


Fig. 8—Typical potential/time curves of specimens of steel 18–8(*b*) and 18–10–3Mo–Ti loaded when indicated after a period of exposure to corrosion alone; 48,000 lb/in², 153–154° C

was being set up in the condensers. Steel 18–10–Nb at 48,000 lb/in² (Fig. 7*b*) showed some continuous extension, probably creep, after the temperature had become stable. Similar slow extension has been noted for steel 18–8–LC(*b*) at similar stresses, and for other steels at stresses above the 0·1% proof stress (as measured at room temperature); it does not occur at lower stresses.

Over the period of the rapid potential fall, jerky extension occurred in steps of 5–10 × 10^{-4} cm (too small to show in Fig. 7), of which the frequency increased with time. There was a tendency for the jerks to become greater in the final seconds before fracture, although this may have been because single jerks occurred too close together to be resolved. The total extensions before fracture of the 7·5-cm long specimens were in the range 0·03–0·1 cm, for any one material increasing with increase of applied stress.

One of the experiments illustrated, with steel 18–8(*b*) (Fig. 7*a*), shows a discontinuity in both the extension/time and potential/time curves, indicating that after a period of very fast crack propagation

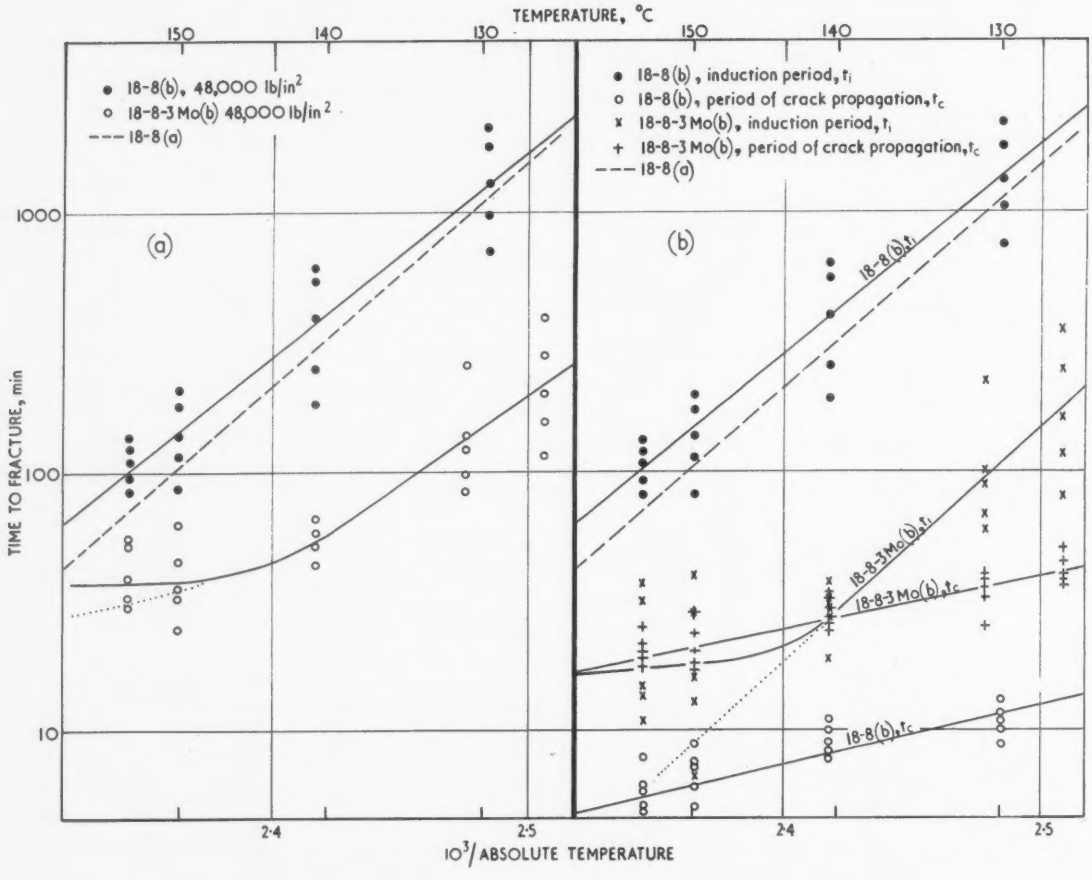


Fig. 9—Variation of time to fracture t_f min with temperature T° K: (*a*) steel 18–8(*a*), 48,000 lb/in²; (*b*) steels 18–8–LC(*a*) and 18–8–3Mo(*a*), 48,000 lb/in²; 18–8–3Mo(*a*), 24,000 lb/in²; 18–8–Ti(*a*), 64,000 lb/in²

(induced by an accident in the apparatus that produced an impact stress) no further advance occurred for a considerable time during which the potential was almost steady.

The results described above indicate that there is an induction period, which can be lengthy, before cracking begins. Cracking does not begin until the last few minutes before fracture of the specimen. In the induction period, corrosion reactions occur, similar to those found on unstressed specimens: the oxide film is first repaired and then breaks down. At the moderate applied stress used in the above experiments, cracking begins at about the time when general oxide-film breakdown sets in, although at higher stresses it is initiated earlier and at low stresses it is not initiated until long after.

If this hypothesis is correct, the presence or absence of stress should be substantially immaterial during the induction period of preliminary action by the corrosive medium; the following experiments were therefore performed.

Prior Exposure to Corrosion followed by Stress Application—Specimens of steel 18–8(*b*) were cleaned and degreased as usual, and exposed for 75 min to hot 42% magnesium-chloride solution without being stressed, potential readings being taken throughout this period. The specimens were then loaded to 48,000 lb/in² and their potentials were followed until fracture. Other specimens of the same steel were treated similarly, except that the load was not applied to any specimen until its potential/time curve showed that the potential maximum had been just passed. Similar experiments were performed on steel 18–8–3Mo–Ti.

The potential/time curves obtained (Fig. 8) confirm that an applied stress of 48,000 lb/in² scarcely influences the overall process until the potential maximum is reached, i.e. until film-breakdown conditions are established. With specimens loaded some time before the potential maximum had been reached, the potential fell slightly at the instant of loading, presumably because of new cracks in the surface oxide film, and then rose to a maximum with a final rapid potential fall, in the same way as the potentials of specimens stressed throughout. Specimens loaded when the potential maximum had been just passed

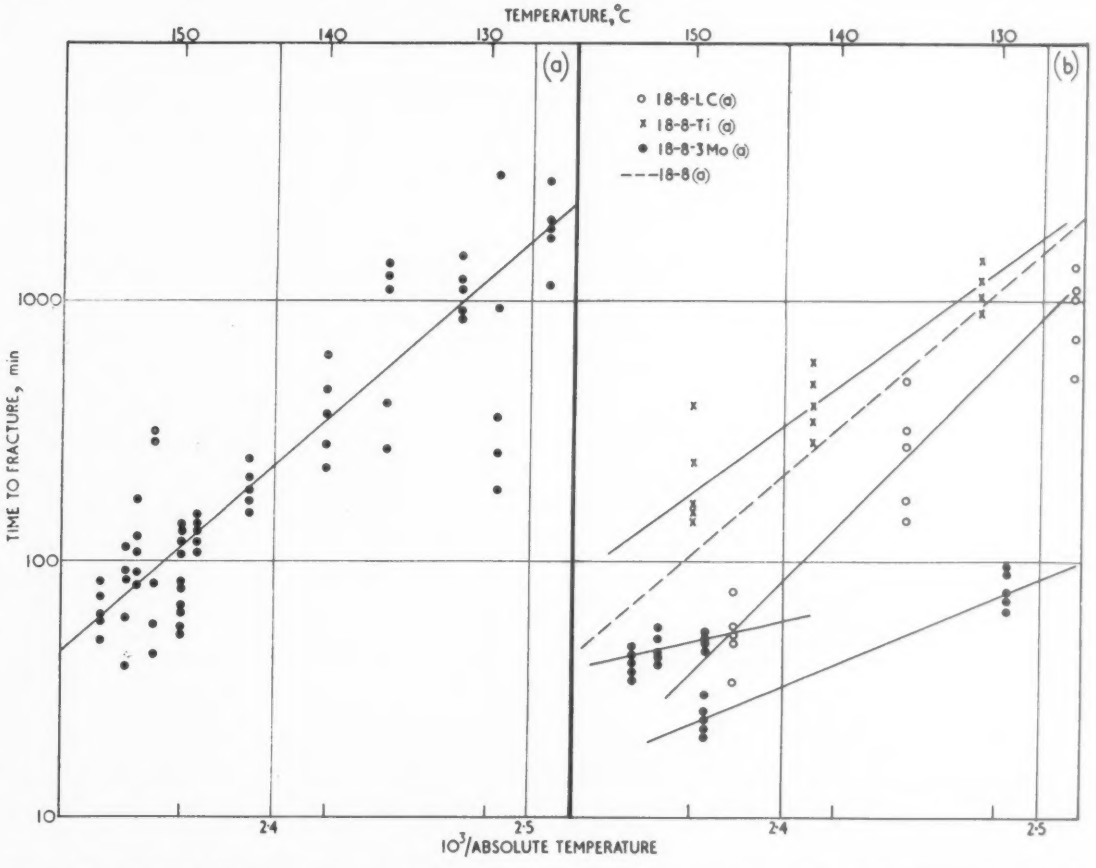
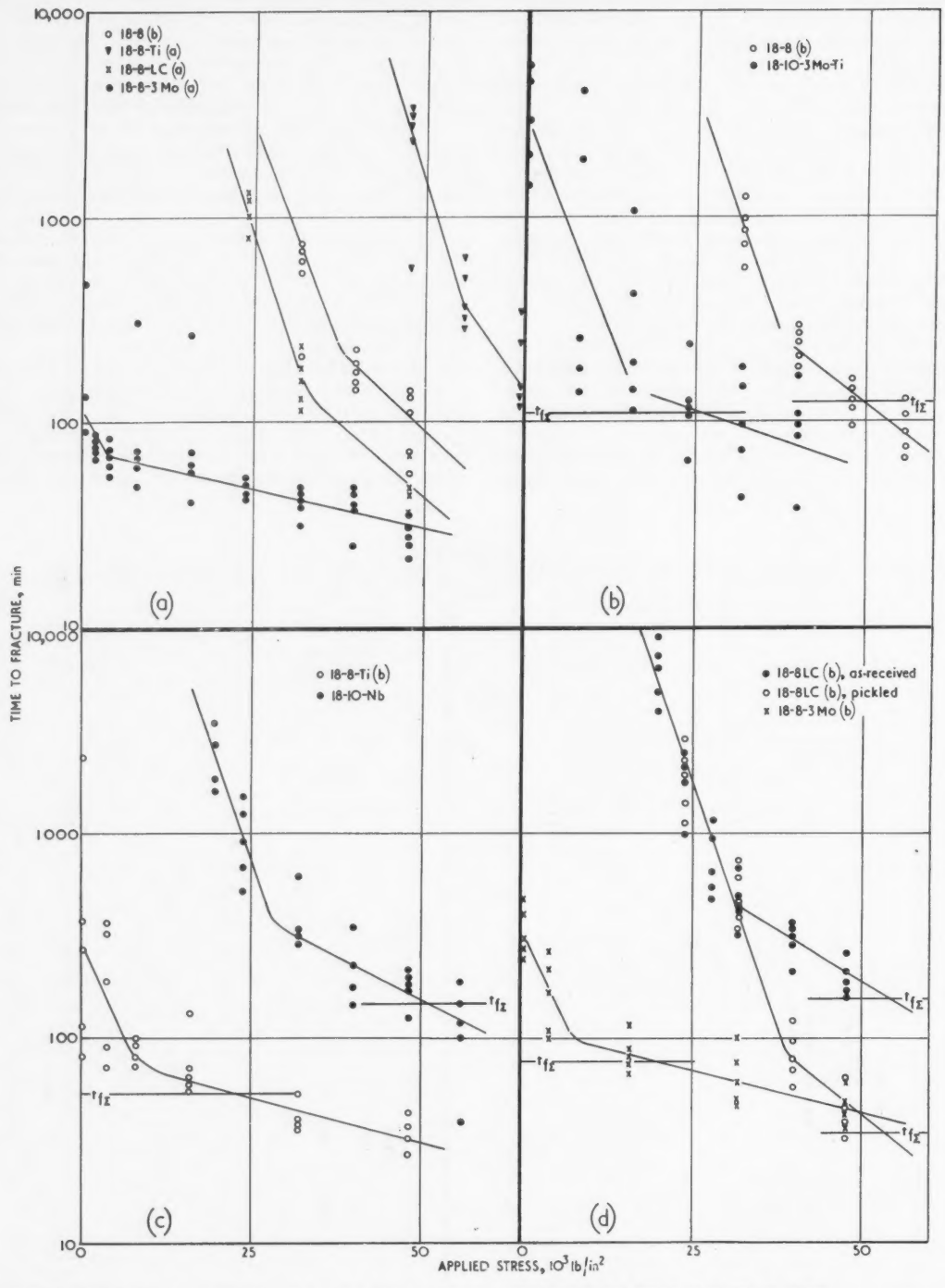


Fig. 10—Variation of (*a*) time to fracture t_f min, (*b*) induction period t_i min, and period of crack propagation t_c min with temperature T° K: steels 18–8(*b*) and 18–8–3Mo(*b*), 48,000 lb/in². Extrapolated data shown by broken line (*see* text)



(*a*) Steels 18–8(*a*), 154° C; 18–8–LC(*a*), 152° C; 18–8–Ti(*a*), 153° C; 18–8–3Mo(*a*), 151° C
(*b*) Steels 18–8(*b*), 153° C; 18–10–3Mo–Ti, 152° C
(*c*) Steels 18–8–Ti(*b*), 152° C; 18–10–Nb, 154° C
(*d*) Steels 18–8–3Mo(*b*), 153° C; 18–8–LC(*b*), as-received, 153° C, and pickled with 6-day room-temperature film, 152° C

Fig. 11—Variation of time to fracture t_f min with applied stress S lb/in²; $t_{f(\Sigma)}$ is defined in the text

failed very shortly after loading, the fall in potential on loading being more pronounced.

These experiments strongly support the hypothesis that applied stress becomes an important factor in the stress-corrosion process only after an initial period during which the metal surface is acted upon by corrosive influences only.

Influence of Variations in the Experimental Conditions on the Resistance of the Steels to Stress-Corrosion Cracking, and on the Form of the Potential/Time Curves

Influence of Temperature

Preliminary experiments on the influence of temperature on the time to fracture of steels 18–8(*a*), 18–8–LC(*a*), 18–8Ti(*a*), and 18–8–3Mo(*a*) were conducted on the as-received materials, cleaned and degreased, stressed at 48,000 lb/in², at 120–155° C. Figure 9*a* shows the variation of the time to fracture, t_f, with absolute temperature, T, for steel 18–8(*a*); the straight line is the statistically calculated regression line of $\log_{10}t_f$ on $1/T$. Figure 9*b* gives the similar results for the other three steels: it may be seen that the temperature dependences of $\log_{10}t_f$ for steels 18–8(*a*), 18–8–LC(*a*), and 18–8–Ti(*a*) are the same within experimental error, whereas that for steel 18–8–3Mo(*a*) is apparently considerably smaller.

Further experiments on steels 18–8(*b*) and 18–8–3Mo(*b*) under the same conditions were made, with potential measurements: the results, as well as giving information of the separate temperature dependences of the induction period before cracking begins, t_i, and the period of crack propagation as indicated by the rapid potential fall, t_c, provide an explanation of the above difference. The potential measurements showed that at 150° C the major part of the time to fracture of steel 18–8(*b*) was t_i, t_c being relatively small. Figure 10*a* shows the plot of $\log_{10}t_f$ and Fig. 10*b* those of $\log_{10}t_i$ and $\log_{10}t_c$ against $1/T$: it may be seen that the temperature dependence of the period of crack propagation is considerably smaller than that of the induction period. Figures 10*a* and *b* also show the similar data for steel 18–8–3Mo(*b*): it may be seen that the temperature dependence of the period of crack propagation is the same as with steel 18–8(*b*), as are those of the induction period and the total time to fracture below 140° C; but at higher temperatures the latter periods are apparently almost independent of temperature. However, the time taken for the apparatus to reach thermal equilibrium is about 10 min when it is operated nominally at 150° C, and the actual induction period for steel 18–8–3Mo(*b*) is only about 15 min; consequently, the true induction period at 150° C, and the total time to fracture, are overestimated in the test. Values for the true induction periods at 140° C and over may be calculated by extrapolation of the lower-temperature $\log_{10}t_i/\frac{1}{T}$ data, and used together with the experimental values of t_c to compute true values of t_f. When this is done, the temperature dependence of t_f is found to be nearly the same as that of t_c, considerably smaller than that of t_i.

Evidently, the temperature dependence of the total time to fracture is largely determined by that of the induction period, just as t_f itself is largely made up of t_i, when $t_i \gg t_c$. But when $t_c \gg t_i$, t_c becomes the major part of t_f, as with steels 18–8–3Mo(*a*) and (*b*) at temperatures over 140° C and probably with steel 18–8–Ti(*b*) at temperatures over 150° C, and the temperature dependence of the total time to fracture is then largely determined by that of the period of crack propagation.

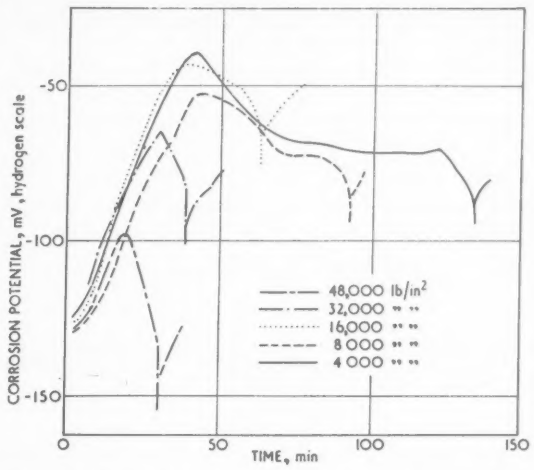


Fig. 12—Typical potential/time curves for various applied stresses: steel 18–8–Ti(*b*), 151–154° C

The temperature dependences of the induction period and of the period of crack propagation correspond to apparent activation energies for the processes of about 40 and 10 kcal/g-mol., respectively.

Influence of Applied Stress

Tests were carried out over a range of stresses on all the steels: the results are shown in Fig. 11*a–d* as plots of $\log_{10}t_f$ against applied stress, S lb/in². At high stresses the times to fracture of all the steels are similar, and variation of stress has a relatively small influence. At lower stresses the steels fall into the two classes earlier indicated. Steels 18–8(*a*) and (*b*), 18–8–LC(*a*) and (*b*), 18–10–Nb, and 18–8–Ti(*a*) show considerably increased times to fracture at low stresses. Steels 18–8–3Mo(*a*) and (*b*) and 18–8–Ti(*b*), however, fracture under no load other than the out-of-balance weight of the lever (some 500 lb/in²) in times only slightly longer than those found at 50,000 lb/in². At such low loads friction in the apparatus makes the true value of applied stress uncertain, but it is clear that rapid stress-corrosion cracking can occur under applied stresses that might well be considered negligible in practice. Steel 18–10–3Mo–Ti appears to fall between the two classes, giving a variation of $\log_{10}t_f$ with applied stress similar to those given by the first group of steels, but showing fairly rapid failure at very low stresses in the same way as the second group.

The $\log_{10}t_f/S$ curves are probably of the same general form for all ten steels. There is a general tendency for the influence of variation of applied stress

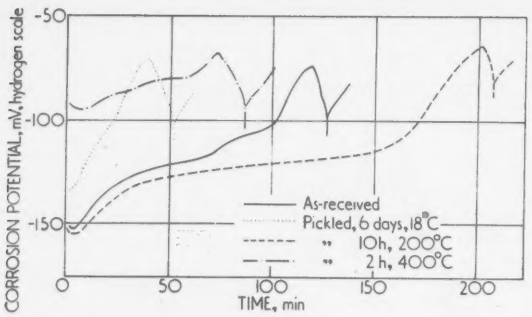


Fig. 13—Typical potential/time curves for specimens treated to give various surface conditions: steel 18–8(*b*), 48,000 lb/in², 153° C

on $\log_{10} t_f$ to be greater at lower than at higher stresses. It seems reasonable to represent the data for all the steels by two merging straight lines, as in Fig. 11.

Typical potential/time curves for steel 18–8–Ti(*b*) at various stresses are shown in Fig. 12. Similar results were obtained for the other steels of the second batch. At the higher stresses, increased stress shortens the induction period before the rapid potential fall, the form of the potential/time curve before the fall being unaltered. Crack propagation begins before film-breakdown conditions, as found in unstressed and lightly stressed material, have been established. At the lower stresses, film-breakdown conditions become established before crack propagation starts, and a period of nearly steady potential occurs between the potential maximum and the final rapid potential fall. Decrease of stress has no influence on the period of film repair, and only a slight influence on the duration and extent of the rapid potential fall, but is associated with a considerable increase in the period of steady potential.

When the induction period, t_i, corresponds to the period of film repair (to the potential maximum) of unstressed specimens, $t_{r(0)}$, the time to fracture can be expressed as $t_{f(\Sigma)} = t_{r(0)} + t_{c(\Sigma)}$, where $t_{c(\Sigma)}$ is the mean time of crack propagation at the stress Σ that makes $t_i = t_{r(0)}$. Values of $t_{f(\Sigma)}$ so obtained from the appropriate potential/time curves are shown, where available, on the $\log_{10} t_f/S$ curves of Fig. 11: $t_{f(\Sigma)}$ is always somewhat shorter than the time to fracture corresponding to the change of gradient of the $\log_{10} t_f/S$ curves. It may well be that the change in influence of applied stress on time to fracture is associated with the change of the preliminary corrosion damage from the very slight metal attack that must occur during the film-repair process (to provide the solid compounds for repairing the film) to the relatively extensive corrosion that occurs after film breakdown. In the two cases (steels 18–8–LC(*b*) and 18–10–Nb) where $t_{f(\Sigma)}$ is considerably shorter than the change-over point in the $\log_{10} t_f/S$ curves, the extent of crack formation is considerably less than for the other steels.

The above results are in general agreement with the hypothesis that the stress-corrosion process can be divided into stages: the production of corrosion damage, and the initiation and propagation of cracks from the points of corrosion. Increase of stress has little influence on the preliminary corrosion process, except slightly to increase the corrosion currents throughout, probably through cracks produced in the surface oxide film by mechanical strain, and through a small stimulation of the anodic electrode process proper. At high applied stresses, the very small attack of the metal surface that must occur during the film-repair process is sufficient to initiate cracking, but at low applied stresses the relatively large attack that occurs after film breakdown is necessary. The two sections of $\log_{10} t_f/S$ curves correspond approximately to cracking initiated before and after general film breakdown.

Variation of applied stress produces only a slight effect on the period of crack propagation. Probably the small decrease in this period with increase of applied stress is caused by the decreased crack penetration necessary before ordinary mechanical tensile fracture of the uncracked metal can occur; the rate of crack propagation may well be independent of applied stress.

The failures observed at ' zero ' applied stress are probably due to the steels, as received, having undergone a small degree of cold work before test. It is likely they contained internal stresses—particularly the duplex steels, in which quenching stresses also may be considerable. Work on steels heat-treated in the laboratory, in progress, indicates that similar failures do not occur in fully softened specimens.

Influence of Surface Oxide Film

Specimens of steel 18–8(*b*) were pickled to remove the existing oxide film, and then given treatments chosen to produce oxide films of different thicknesses. The specimens were pickled in an aqueous solution of nitric and hydrofluoric acids containing 15% HNO_3 and 2% HF at 50° C for 5 min, washed in warm water, rinsed in acetone, and allowed to dry. One group of specimens was left in a desiccator at room temperature for 6 days, another was heated in air for 10 h at 200° C, and a third for 2 h at 400° C Specimens were then degreased and tested at 48,000 lb/in². The results are given in Table III. The specimens bearing the room-temperature film and those bearing the visible 400° C film fractured sooner and showed more extensive cracking, while those bearing the 200° C film fractured later and showed less extensive cracking, than specimens tested as received. The 400° C film showed visible interference colours that disappeared during the test; it is probable that this film was cracked or porous and largely removed during the test.

The potential/time curves (Fig. 13) show that the difference in the times to fracture of the different groups of specimens lies almost entirely in the induction period; as expected, with different surface oxide films, different periods are required for the production of the preliminary corrosion damage necessary for crack initiation. The duration of the final rapid potential fall is somewhat greater for the specimens carrying the room-temperature and 400° C films than for those having the 200° C film : the former specimens showed the more extensive cracking, in harmony with the tendency already noted (Table II)

for the extent of cracking to increase with increase of the period of crack propagation.

A series of experiments was performed on steel 18–8–LC(*b*) over the whole range of applied stress, with pickled specimens carrying the six-day room-temperature film; the results have already been shown in Fig. 11*d*. The times to fracture at high stresses were shorter than those of the ' as-received ' specimens, but at low stresses the behaviour of the two kinds of specimens was similar. The stress corresponding to the change-over point of the $\log_{10} t_f/S$ curve was increased from about 30,000 to about 40,000 lb/in². Potential/time curves showed that $t_{f(\Sigma)}$ (Fig. 11*d*) was considerably less with the ' pickled, 6-day film ' specimens. Evidently, these specimens had thinner initial oxide films, so that the film-repair process proceeded more rapidly than with the ' as-received ' specimens and the times to fracture at high stresses were shortened; but the initial oxide film had little influence on the long-term corrosion reactions, so that the times to fracture at low stresses were unaffected.

Influence of Composition of the Corrosive Solution

Purity of Magnesium Chloride—Some batches of the ' pure crystals ' grade of magnesium chloride contained insoluble matter; the solutions were decanted and, if necessary, filtered before use to remove it. The material could also be removed by the addition of a little hydrochloric acid; provided that only just sufficient acid to dissolve it was added, different batches of salt gave similar stress-corrosion cracking results. About 0·02% v/v of concentrated acid in 42% w/w magnesium-chloride solution produced a greenish colour, removable by boiling. Solutions made with ' Analar ' grade magnesium chloride did not give the green colour on acidification; however, since they gave precisely the same stress-corrosion-cracking results as the standard magnesium-chloride solutions, the impurity leading to the green colour is insignificant with respect to the present stress-corrosion cracking.

Acid Content—A particular batch of magnesium chloride led to unusually long times to fracture in steel 18–8(*b*). The potential/time curves showed that the difference lay in the induction period. Tests were then carried out in a series of solutions to which hydrochloric acid was added in various amounts. Addition of small amounts of acid decreased the induction period and hence also the time to fracture. The solution containing 0·01% of added acid gave approximately the same results (Fig. 14*a*) as solutions made up from previous batches of magnesium chloride. Evidently, the particular batch of magnesium chloride was somewhat alkaline.

Additions of acid greater than 0·02%—the concentration required to produce the green colour—gave considerably different results. The potentials of stressed or unstressed specimens of steels 18–8(*b*) and 18–10–3Mo–Ti over the whole process tended to be more noble than those found with unacidified solutions. The potential/time curves (Fig. 14*b*) showed no initial fall and rise to a maximum: the potential fell slightly to a steady value about 10 min before fracture, and rose slightly afterwards: the negative kick at fracture was almost absent. Unstressed specimens gave similar curves. Extension of specimens

Table III

INFLUENCE OF SURFACE CONDITION ON TIME TO FRACTURE

Steel 18–8(*b*): Pickled in aqueous 15% HNO_3, 2% HF at 50° C for 5 min

Condition	Temp. of Testing, ° C	Time to Fracture, min	
		5 Tests	Mean
As received	150	69, 148, 222, 135, 125	139
Pickled, 6-day room-temperature film	152	41, 46, 35, 29, 59	38
Pickled, then 10 h at 200° C	154	214, 185, 239, 201, 255	218
Pickled, then 2 h at 400° C	153	127, 116, 32, 67, 92	97

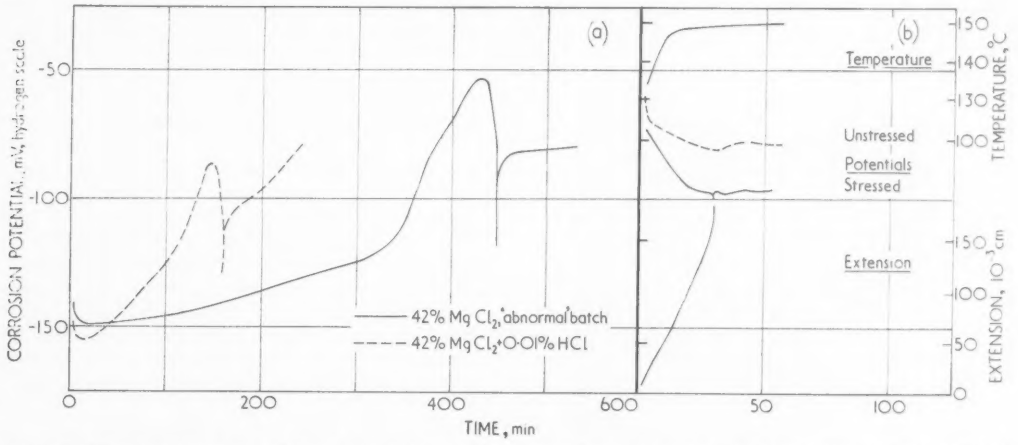


Fig. 14—(*a*) Effect produced by addition of 0·01% of HCl to the abnormal batch of MgCl (steel 18–8(*b*), 152–153° C, 48,000 lb/in²); (*b*) typical potential/time, extension/time, and temperature/time curves in solution containing 0·2 vol.-% conc. HCl (steel 18–8(*b*), 48,000 lb/in²)

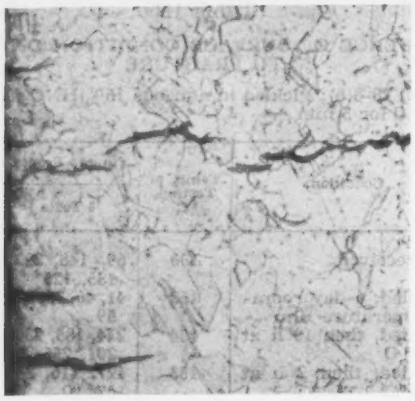


Fig. 15—Typical fine stress-corrosion cracking: steel 18–8 (*b*), 48,000 lb/in², 154° C × 500

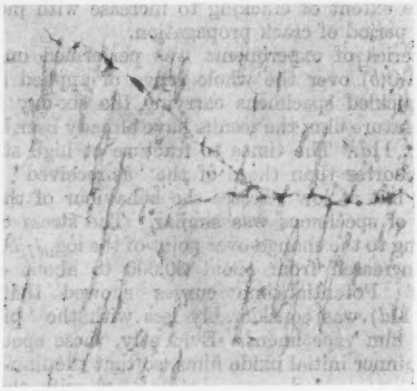


Fig. 17—Branching of a minor crack × 1000

of both steels occurred almost from the start of the test, as shown in Fig. 14*b* for steel 18–8(*b*). The specimens after test showed a layer of blackish-green, loosely adherent corrosion products on the surface; the underlying metal surface was etched and pitted.

Evidently, considerably acidified magnesium chloride solutions cause rapid film breakdown and dissolution, and crack propagation sets in almost from the start, without the relatively long induction period found under ' standard,' non-acid, conditions. It may be noted that the measured potential of the specimen is more noble than in the non-acid solution, although the corrosion currents are clearly much greater; this effect doubtless arises because the defilmed metal is a more effective catalyst for the cathodic reaction, such that the polarization is less even though the current density is greater, and also because cathodic hydrogen-ion discharge as well as the sluggish oxygen reduction is possible from acid solutions.

Metallographic Examination of Specimens after Fracture

Figures 15–19 show typical microstructures and cracking in steels 18–8(*b*) and 18–8–3Mo(*b*); other steels gave similar results. The microstructures are

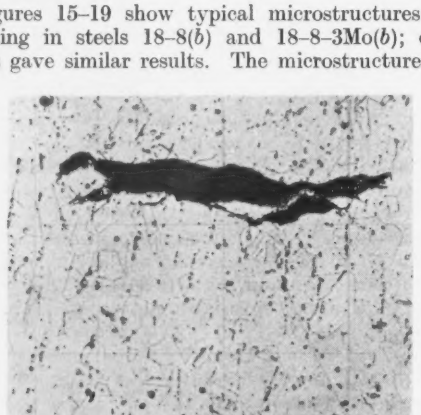


Fig. 16—Yawning cracks near fracture; similar specimen to Fig. 15 × 500

such as would be expected for these steels after their preparation and exposure to the various stress-corrosion cracking conditions: the two 18–8–Ti steels show some quasi-martensite; steel 18–10–3Mo–Ti shows about 1% of ferrite; and steels 18–8–3Mo(*a*) and (*b*) show some 20% of ferrite as stringers in the wire-drawing direction.

Two distinct types of cracking have been observed. In steels 18–8(*a*) and (*b*), 18–8–Ti(*a*), 18–10–Nb, and 18–10–3Mo–Ti, cracking develops on planes substantially perpendicular to the axis of the specimen and little branching occurs. There are many fine cracks (Fig. 15) occurring in groups, extending up to one-eighth of the circumference and penetrating less than one-quarter of the diameter of the wire. Some larger cracks, particularly frequent near the point of fracture, extend up to one-fifth of the circumference and penetrate more deeply; they show considerable ' yawning,' and grow more rapidly radially than round the circumference. Figure 16 shows some of these cracks, one of which has apparently grown sideways through the plane of the section. The final fracture occurs by ductile failure starting from several of the larger cracks.

In steels 18–8–3Mo(*a*) and (*b*) cracking is not confined to planes perpendicular to the axis, and there is frequent branching. There are relatively few cracks. Figure 17 shows a typical fine isolated branched crack. The major cracks extend almost completely round the specimen, and show complex branching (Figs. 18 and 19). Steel 18–8–Ti(*b*) shows similar major cracking but with much less pronounced branching.

Cracking is mainly transgranular, but may become intergranular for short distances if a suitably orientated grain boundary is encountered. The tendency to intergranular cracking is greatest with the duplex steels, and there is some indication (Fig. 17) that branching occurs when a crack that is following a grain boundary meets a grain corner providing alternative paths. It is probable that the cracking in the first group of steels is controlled almost entirely by the applied stress, and is thus confined largely to the plane perpendicular to it, but that tesselated stresses associated with strain hardening and the higher proof stress, and with the two-phase structure, are important

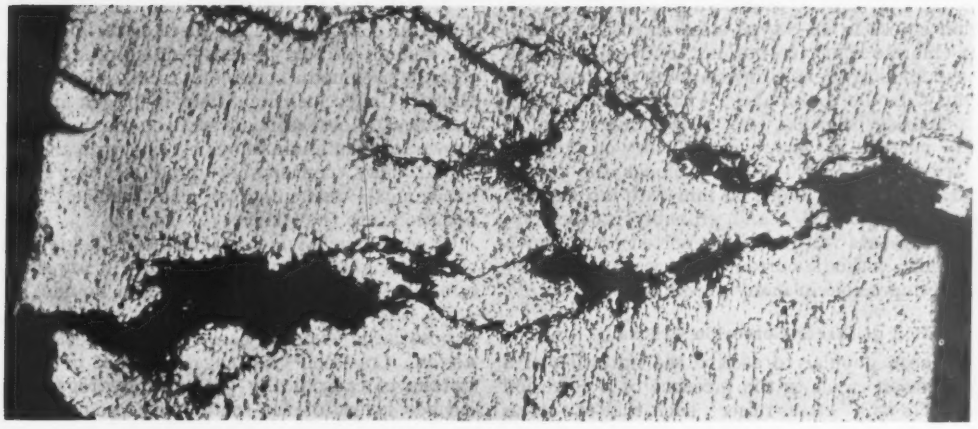


Fig. 18—Complex cracking in steel 18–8–3 Mo (*b*), 40,000 lb/in², 153°C × 250

in controlling cracking in the duplex steels, cracking there proceeding perpendicular to the greatest resolved tensile stress. The tendency to intergranular cracking may well be enhanced by the stresses induced by deformation of adjacent grains.

In the present wire specimens, with stringers of ferrite perpendicular to the main cracking direction (Figs. 18 and 19), cracking does not avoid the ferrite; Edeleanu[6] found that cracking tended to avoid ferrite under different conditions. Thus cracking can occur in ferrite, but possibly less readily than in austenite.

The small size of specimens makes it difficult to obtain a true profile of the surface and of the mouths of cracks. Figure 15, however, suggests cracks starting from shallow pits and it is possible that Fig. 18 shows a similar effect.

DISCUSSION

The present experiments show that the process of stress-corrosion cracking in austenitic stainless steels in hot magnesium-chloride solution consists of consecutive stages—the prior provision of suitable corrosion damage, followed by the initiation and propagation of cracking from that damage.

The Induction Period of Corrosion Damage

Stress plays a minor part in this part of the process, which appears to follow a course similar to the early stages of the localized corrosion of tin[16] and aluminium[18] in chloride solutions, and of mild steel[17] in chloride solutions containing concentrations of anodic inhibitors insufficient to prevent all attack; it is also very similar to the early stages of the stress-corrosion cracking of Al–7% Mg alloy in 3% sodium-chloride solution.[19] The induction period is, however, considerably shortened by increase of temperature. Also, the high chloride concentration used in the present experiments doubtless hastens its completion; the film-repair period in tin corrosion[16] is greatly reduced, and the total time to fracture of 18–8 specimens in Edeleanu's[6] experiments was similarly reduced, by increase of chloride concentration. Since the induction period is in general much longer than the cracking period, it is usually the determining stage in the time to fracture, and its extension so far as possible is of considerable practical importance.

Corrosion processes may in general be retarded in several ways, notably by cathodic or anodic inhibition, by anodic passivation and by cathodic protection. In the present case, cathodic inhibition, either by the complete exclusion of oxygen, or by the presence of ions or molecules that become precipitated or adsorbed on the cathodic film surface, is probably possible in principle: Gilbert and Hadden[19] showed that the onset of the stress-corrosion cracking of Al–7% Mg alloy in 3% sodium-chloride solution may be delayed indefinitely by the rigorous exclusion of oxygen. However, such conditions are unlikely to be realizable in practice. Anodic inhibition is also not likely to succeed in hot aqueous solutions of high chloride concentration: our preliminary experiments with additions of potassium chromate (here limited in amount because of its small solubility) have in fact shown that this anodic inhibitor merely leads to a faster attainment of film-breakdown conditions. Anodic passivation by applied anodic current is also not very hopeful in hot concentrated chloride solutions. Cathodic protection is, however, a promising method of extending the induction period: Mears,

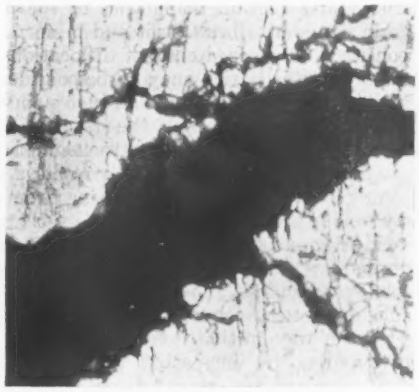


Fig. 19—Part of Fig. 18 × 1000

Brown, and Dix[9] showed that the appearance of stress-corrosion cracks in 18–8-type steels exposed to 10% sodium-chloride solution containing hydrogen peroxide may be indefinitely delayed by coupling the specimens to slightly anodic materials. With hot 42% magnesium-chloride solution, we have found that a cathodic current of about 30 $\mu A/cm^2$ on 18–8(*b*) steel stressed to 48,000 lb/in^2 prolongs the induction period, which is about 90 min with no applied current, to at least 96 h and probably indefinitely; these experiments will be fully reported later. Such small applied cathodic current densities would be by no means impossible in many practical cases.

The Initiation of Stress-Corrosion Cracks

It has been popular to ascribe the initiation of a stress-corrosion crack to the presence of a corrosion pit that acts as a ' stress-raiser ', such that the stress at its base locally exceeds the fracture stress of the material. At first thought, the present results—showing that a longer induction period before crack initiation, with more initial corrosion damage, is necessary for crack initiation with a lower applied stress—appear to support this view, but consideration suggests that they do not.

There is no reason to suppose that when the initial corrosion damage is *more extensive*, the stress-raising effect is necessarily *more pronounced*. Indeed exactly the reverse may occur, when pits formed by corrosion increase in number, or extend sideways rather than in depth; only when the pits remain few and extend in depth rather than sideways—as in certain kinds of intergranular corrosion—can the stress-raising effect become more pronounced with increased corrosion. In the present case there is no evidence of cracking starting from a few deep intergranular pits: on the contrary, it starts most quickly and most generally under the special conditions of low pH where the original oxide film on the metal seems to be very quickly and generally removed, when the induction period is very short. It appears in fact that *the probability of crack initiation depends on the amount of bare metal surface exposed to the electrolyte* by the initial corrosion reactions. It is not at all unlikely that certain special features of a bare metal surface, that occur only rarely from an atomistic point of view, are the sites of crack initiation. Apart from the grain boundaries and sub-grain boundaries of the matrix alloy, grain boundaries between it and minor heterophase constituents, surface ends of dislocations, and strained parts of the lattice, may all be considered as possible sites of crack initiation. At such points, one may reasonably suppose that the rate of anodic dissolution of metal under joint electrochemical action and quite moderate stress is especially rapid; alternatively, mechanical deformation starting from such sites may well be greatly assisted by the prior removal of the oxide film, as suggested by the experiments of Andrade and his colleagues.[22] Consequently, a true stress-raising ' crack-pit ' may begin to form, and, advancing on a very narrow front, proceed in an ' autocatalytic ' way, in that it continually accelerates its own progress by increasing the stress at its advancing edge, which, as discussed later, greatly stimulates crack propagation.

It may also be remarked that the extent of bare metal in contact with the electrolyte is linked with the amount of acid present—either produced locally through the anodic corrosion processes or deliberately added. Under such conditions hydrogen may well be produced at local cathodic spots on the bare metal—as shown for aluminium alloys by Edeleanu and Evans[18]—and may conceivably produce very localized mechanical weakening of the surface layers by diffusion into the lattice.

There is much evidence that metals (e.g. mild steel) suffer straightforward tensile failure at lower fracture stresses when they are immersed in liquids, such as acids, that remove the oxide film and consequently expose large areas of bare metal to electrochemical action. Cracknell[23] has suggested that in some cases uptake of cathodically produced hydrogen into the surface layers of the metal may produce sufficient weakening to account for the lowered fracture stress, and Evans[24] that a similar mechanism may be important in crack propagation in stress-corrosion cracking. It may well be that in some cases cathodic hydrogen, in others enhanced anodic attack at special parts of the lattice, and in yet others both phenomena, are responsible for assisting an applied stress to initiate cracking. Experiments with various cathodic and anodic currents applied to metal under film-free conditions, and with various inhibitors and stimulators of hydrogen diffusion, should help to elucidate this point.

Whatever the exact nature of the points on the bare metal surface that are able to initiate cracking under applied stress, they are doubtless of varying effectiveness. It is reasonable to suppose that as the corrosion reactions proceed, points of increasing effectiveness are exposed, and/or that the effectiveness of all the points is increased by increased hydrogen absorption. Consequently the lower applied stress necessary for crack initiation when the induction period of corrosion damage is longer is readily explained.

The experiments on specimens exposed to corrosion alone until film-breakdown conditions have become established, and then stressed, provide some support for the hypothesis. When the stress throughout is less than $t_{r(0)}$, crack propagation starts only after a short interval (a few minutes) after the stress is applied, although the amount of corrosion damage present is probably as great as that occurring before cracking in specimens stressed throughout. It may well be that this interval represents the time taken for ' crack-pits,' activated by stress, to form by modification of the unstressed corrosion damage.

The Propagation of Stress-Corrosion Cracks

With the present direct-loaded 0·02-in. dia. wire specimens, the time of crack propagation is 5–30 min under a wide variety of conditions. Since the crack leading to fracture has usually penetrated upwards of half-way through the specimen when final mechanical parting of the remaining cross-section occurs, this time corresponds to a net rate of crack propagation of the order of 0·5–3·0 mm/h; other workers have observed similar rates, which are much too slow for a purely mechanical parting process, but, at first

thought, very rapid for purely electrochemical dissolution. Consequently, it has been suggested, notably by Keating[25] and by Evans,[26] that cracking proceeds by alternate rapid mechanical and slow electrochemical increments, rapid mechanical parting occurring along paths of weakness until a relatively strong region requiring slow electrochemical removal is reached.

Dix[27] suggested that cracking proceeds along paths especially susceptible to corrosion, and Waber[28] has pointed out that such a path might be propagated by the action of the strain immediately ahead of a crack. In austenitic stainless steels, strain can produce austenite transformation by diffusionless change to 'quasi-martensite,' which Rocha,[29] Waber,[28] and Edeleanu[6] have suggested as the susceptible path: Edeleanu[6] showed that needles of such quasi-martensite are preferentially attacked by hot magnesium-chloride solution and that cracks and quasi-martensite are often spatially associated. However, some austenitic steels that undergo stress-corrosion do not form quasi-martensite on straining; there is considerable doubt whether quasi-martensite can be so formed in any 18–8-type steel at about 150° C, at which temperature some of the wires in the present experiments were subjected to stress for the first time; and cracks passing through ferrite grains have often been observed. It thus seems to us likely that the quasi-martensite found at the site of some transgranular stress-corrosion cracks through austenite is incidentally produced by the high stress and strain there but is not needed for crack propagation—although quasi-martensite may provide one kind of site for crack initiation.

There is, of course, no doubt that stress and probably strain at the advancing edge of a crack play an essential part in the cracking process: if the load is removed during cracking, cracking stops. There is equally no doubt that electrochemical anodic action plays an essential part: in preliminary cathodic protection experiments similar to those mentioned earlier, we have found it possible to stop cracking indefinitely *after crack propagation has begun* by an applied cathodic current (about 170 $\mu A/cm^2$) sufficient to bring the specimen potential down to about −150 mV (N hydrogen scale), which prevents the specimen surface acting cathodically towards the anodic cracks. A similar effect has been reported by Edeleanu[12] for the stress-corrosion cracking of aluminium alloys.

Rather than the 'alternate' mechanical–electrochemical mechanism of crack propagation, we believe that the evidence at present points to a *joint* mechanical–electrochemical mechanism. Although, as pointed out by Uhlig[30] and others, the thermodynamic reactivity of a metal anode can be only very slightly influenced by the presence of stress or strain energy, it is certain that the *rate* of anodic dissolution can be considerably increased by the presence of stress and/or strain, as shown very clearly for mild steel by Simnad and Evans.[31] Stress may so act by disrupting an otherwise readily formed, passivating oxide film on the anode, and/or by diminishing the energy hump for the actual anodic removal of metal cations from the lattice; it may be significant that many cases of stress-corrosion cracking occur with alloys and

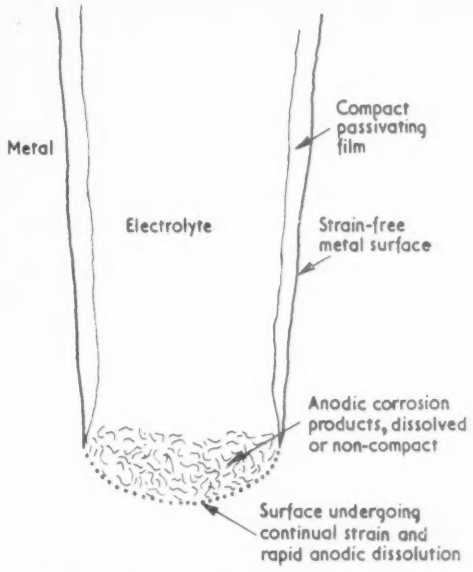


Fig. 20—Mechanism of crack propagation (schematic)

environments that readily form passivating oxide films (e.g. austenitic steels and aluminium alloys in chloride solutions, mild steel in caustic alkali) and/or that have rather high anodic polarization in the unstressed condition (iron alloys generally).

The advancing edge of a crack may be pictured as shown schematically in section in Fig. 20. On the fractured sides of the crack, film growth is proceeding, as indicated by the rapid rise of potential of completely fractured specimens. At the advancing edge, the raised stress and/or strain puts the metal atoms in an especially favourable state for easy anodic reaction, and, even if in so doing they form sparingly soluble products, these are mechanically disrupted and prevented from forming a blocking compact film. Provided that there is a quite small degree of yielding to give yawning of the crack, sufficient electrolyte solution is continually sucked in to maintain the process.

It may be of interest to calculate the order of magnitude of anodic current required at the advancing edge of a crack to account on the above model for the observed rates of propagation. If we assume that iron, chromium, and nickel of average density 7·5 dissolve to bivalent ions of approximate average equivalent weight 28, a rate of 2 mm/h corresponds to an anodic current density of 1440 mA/cm^2; such a current density and penetration rate can be achieved on ferrous anodes under conditions that prevent the formation of passivitating films.* If we further assume that the advancing edge of a crack is 0·02 cm long and—as a liberal estimate—1000 Å wide, the current causing crack propagation is some

* For example, iron and steel may be anodically dissolved in acids at current densities equivalent to penetration rates of over 1 cm/h provided that the accumulation of compact corrosion products is prevented.[32]

28×10^{-2} μA per crack. If current is flowing down the crack to the advancing edge, as postulated, there will be also a relatively small anodic current density (say an average of 0·2 mA/cm²) assisting film growth on the newly fractured sides of the crack, and this (owing to the relatively very large area, say $2 \times 0{\cdot}02 \times 0{\cdot}02 = 0{\cdot}0008$ cm²) might account for a further 16×10^{-2} μA entering the crack. If then there are 100 cracks per cm² of wire surface, the total cathodic current density required from the surface is some $(28 + 16) \times 10^{-2} \times 100 = 44$ μA/cm². Experimentally, we find that a lowering of potential of the same order as that found during crack propagation, 10–30 mV, is produced when an unstressed specimen, made cathodic, operates at a current density of some 20–60 μA/cm². Such a current is then sufficient to account for the propagation of 100 cracks per cm² of specimen surface at the observed rates, if it is assumed as above that the advancing edge of each crack, where the raised stress and strain are sufficient to maintain anodic dissolution at some 1500 mA/cm², is some 1000 Å wide. If, as is quite possible, the advancing edge suffering rapid anodic dissolution is much narrower, correspondingly less current is required for crack propagation, and more is available for film growth on the sides of the crack; the current propagating the advancing edge may indeed be only a small fraction of the total current entering the crack.

The hypothesis can clearly be tested further by experiments to find the influence of applied cathodic and anodic currents, insufficient to give respectively complete cathodic protection or anodic passivation, on the rate of crack propagation under various applied stresses; we are proceeding with this work. Meanwhile, it is of interest to note (Table II) that when many cracks are initiated, the rate of crack propagation is in general lower than when few are initiated, as would be expected if the available current is shared by all the cracks—a state of affairs very similar to that demonstrated quantitatively for the pitting corrosion of aluminium by Mears and Brown.[33]

The discontinuous nature of the extension during crack propagation seems to be a genuine phenomenon, but it is by no means evidence that crack propagation itself proceeds discontinuously. The total extensions found in the present experiments are of the order of 0·03–0·1 cm, the 'jerks' being each about 5×10^{-4} cm, on specimens 7·5 cm long. Since in many cases only a small part of the length of a specimen suffers cracking, and since some of the cracks may be seen to remain opened out on specimens after testing, most of this extension is associated with plastic deformation. It is quite possible that the local yield point in the alloy varies from grain to grain. Thus, when a particular crack or crack system approaches a region of locally low yield point, the metal there deforms plastically until strain hardening halts the process. Discontinuous extension can, in fact, sometimes be observed in alloys that are not cracking; it may well be a feature of the tesselated nature of polycrystalline material—being merely displayed prominently by the random cracking process—rather than a feature of crack propagation itself.

The joint mechanical–electrochemical mechanism for crack propagation just proposed derives features from many previous models, notably the 'generalized theory' of Mears, Brown, and Dix,[9] the 'alternative' mechanism of Keating[25] and Evans,[26] the 'susceptible path' of Dix,[27] Waber,[28] and Edeleanu,[6] and the 'film breakdown' of Logan.[20] Its distinctive feature is the concept of stress- and especially strain-assisted anodic dissolution at high current density of material that quickly becomes anodically passivated in the absence of high stress and plastic strain.

Practical Considerations

In practice, the *alleviation* of stress-corrosion cracking is of little value—the onset of cracking must be eliminated or at least very greatly delayed. Although a reduction of applied stress or a relief of internal stress delays the onset of cracking, it does not prevent it indefinitely; also, it appears that small amounts of prior plastic strain may lead to early cracking at very low applied stress. It is certainly very doubtful if for any material there is a threshold stress below which cracking will never start. Moreover, the uncertain values of the residual stress and strain in constructions generally, and the difficulty of estimating the degree of strain hardening that inevitably occurs during fabrication, would almost always make it impossible to lay down any quantitative guidance even for a material for which a threshold value applying to a specific condition had been found. For the 18–8-type stainless steels, the results given above show that moderate composition variations are probably of little significance except in so far as they influence the mechanical properties, which are also greatly influenced by plastic strain; also, specific heat-treatments or degrees of strain-hardening that would eliminate or greatly delay the onset of stress-corrosion cracking seem unlikely to be found.

On the other hand, the present experiments suggest that stress-corrosion cracking at least of the present type may be avoided in practice by the adoption of means to reduce the corrosion damage suffered during the induction period. Even when conditions of temperature and corrosive medium cannot be ameliorated, as is commonly the case in chemical plant, the initial corrosion damage may very probably often be kept down to a safe degree by means of some form of cathodic protection. Only very small cathodic current densities and a lowering of potential of some 50–100 mV appear to be necessary—indeed larger current densities and too low potentials may well be undesirable. We shall return to this topic in a further paper.

SUMMARIZED CONCLUSIONS

(1) The stress-corrosion cracking of 18–8-type chromium–nickel steels in hot aqueous magnesium-chloride solutions takes place in stages, which may be demonstrated by following the electrode potential and the mechanical properties of steel specimens during the stress-corrosion cracking process. There is an induction period of corrosion damage, not greatly influenced by the presence of applied stress or by its value, followed by the initiation of cracks at points on the bared metal surface and their rapid propagation

roughly perpendicular to the greatest resolved tensile stress.

(2) The induction period includes a period of oxide-film repair and may also include a period of film breakdown and pitting corrosion. In 42% w/w aqueous $MgCl_2$ at 154° C, it is of the order of 20–200 min for 'softened' 18–8 steels (slightly cold-worked in handling), stressed to 40,000–50,000 lb/in². It is slightly shortened at higher applied stresses, and much lengthened at lower. It is considerably shortened by increase of temperature over the range 135–155° C. It is shortened to a few seconds by the acidification of the corrosive medium with 0·02% hydrochloric acid (which removes the initial oxide film) and indefinitely lengthened by mild cathodic protection.

(3) There is evidence that cracks are not initiated at 'stress-raising' corrosion pits, but rather that they begin at points on the bared metal surface that are especially anodically active.

(4) Cracks are propagated at a rate of some 0·5–3·0 mm/h. This rate is somewhat enhanced by increase of temperature, but is almost independent of applied stress over a wide range. Cracking can be arrested by cathodic protection applied *after* it has begun: consequently it involves electrochemical anodic dissolution. Although the extension of direct-loaded specimens while they are cracking is discontinuous, there is no evidence that crack propagation itself is discontinuous: the experimental evidence is consistent with propagation by the continuous anodic dissolution of the highly stressed and strained metal at the edge of the crack, advancing on a front up to 1000 Å wide, with concomitant anodic passivation of the unstressed fractured sides of the crack.

(5) It is unlikely that practical problems of stress-corrosion cracking of 18–8-type chromium–nickel steels can be solved by attention to alloy composition, by heat, mechanical, or surface treatments, or by minimizing applied stress. On the other hand, means of reducing the corrosion damage, especially cathodic protection, appear to offer considerable promise.

Acknowledgments

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Corrigendum: Figures 9 and 10, on pages 132 and 133, have inadvertently been transposed. On Fig. 9*b*, the lower 18–8–3Mo (*a*) curve is for tests at 48,000 lb/in² and the upper for 24,000 lb/in².

Effects of Sulphate–Chloride Mixtures in Fuel-Ash Corrosion of Steels and High-Nickel Alloys

By H. T. Shirley, B.Sc., A.R.C.S.

SYNOPSIS

Severe attack on heat-resisting steels and high-nickel alloys at around 700° C in equipment handling ash and combustion products of peat and oil fuels has been found to be associated with sulphur absorption. This type of attack is shown to result from contact with alkali and alkaline-earth sulphates contaminated with chloride, but not through direct absorption of sulphur from gaseous products of combustion.

A study has been made of the effect of composition and temperature for a range of alkali and calcium sulphate–chloride mixtures, and has included tests with various heat-resisting materials, particularly 337 and F.C.B.(T) steels and Nimonic 80A. Micrographic characteristics of the attack have been studied. 1118

Introduction

GAS-TURBINE DEVELOPMENT and the desire to use vanadium-bearing fuel oils has, in recent years, focused considerable attention on vanadium ' attack ' within the range 600–800° C.[1, 2] To some extent this has diverted attention from other aspects of fuel-ash corrosion, one of the most serious of which is the attack by sulphate–chloride mixtures, particularly from alkali sulphate contaminated with chloride.

In 1949 a case of intense wastage of 25/20 chromium–nickel steel superheater supports in the boiler of a large liner was found to be associated with excessive contamination of the fuel oil with sea water. An example is shown in Fig. 1. This sample was heavily encrusted with a whitish deposit in which sodium sulphate was predominant. Evidence was obtained of the liability to severe attack at 850–900° C from such deposits, but the overriding importance of small amounts of chloride impurity was not appreciated.

Subsequent work relating to oil fuel ash was described in a paper prepared in connection with the 1951 Symposium on High-Temperature Steels and Alloys for Gas Turbines.[3] This included various steels in contact with sodium sulphate, contaminated over part of the 500-h testing period with 0·3% sodium chloride. Whereas the plain sodium sulphate had little effect on the rate of scaling, contact with the chloride-contaminated material caused rapid increase in the corrosion rate. For example, over three 20-h test cycles, 18/11 chromium–nickel–niobium steel suffered six times as much attack in the impure as in the pure salt, whilst the addition of 0·5% sodium chloride increased the attack 25 times above that with the pure sulphate.

At the time this danger was overshadowed by the vanadium problem, but later tests with equipment burning non-vanadium fuels have shown it to be an aspect requiring very careful study. In particular, attack by peat ash in certain gas-turbine equipment showed that it could in some circumstances have an extremely serious effect on both steels and nickel-based alloys.

EXAMINATION OF SAMPLES FROM SERVICE TRIALS

Samples of gas-turbine air-heater tubes were examined after 1000 h operation at 770° C with peat fuel. The three materials particularly relevant to the present study were steels 337 and F.C.B.(T), and the high-nickel alloy Nimonic 80A, their compositions being similar to those given for these materials in Table VI.

Attack on the 337 tubes was reported to be uniform with few deposits. F.C.B.(T) steel was attacked uniformly, with some variation over large areas, but attack on the Nimonic alloy was characterized by warts covering severely pitted zones. Complete penetration occurred in some cases within $\frac{1}{4}$ in. of very lightly attacked material. Laboratory examination of the samples confirmed the uniformity of attack on 337 steel, with losses of the order of 0·015 in. from an initial wall thickness of 0·064 in. $\pm$ 10%. F.C.B.(T) steel had been uniformly attacked to a much smaller extent, retaining a wall thickness of 0·058–0·068 in., but Nimonic 80A showed very severe local attack with penetration to more than 0·040 in. in places.

The attack was in all cases associated with sulphur pick-up, averaging 0·5% in the first few thousandths of an inch from the surface. Sections through 337 and F.C.B.(T) tubes are shown in Figs. 2*a* and *b*, and for a Nimonic tube in Figs. 3*a* and *b*. Work in the Mond Nickel Research Laboratories* has shown the sulphur pick-up by the Nimonic to be associated with the production of Cr_2S_3. Similar general characteristics were found for the attack on Nimonic 80A blades from an open-cycle peat-fired turbine (Fig. 4), and have been observed in samples from oil-fired boiler installations. Coal ash appears much less corrosive, but some development of this type of attack has been observed after 1000 h in the range 700–750° C with coal fuel.

PRELIMINARY LABORATORY TESTS

Tests relating to direct sulphur absorption from gaseous sulphur products are summarized in Table I. These were carried out in the apparatus described in

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Mr. Shirley is with the Brown-Firth Research Laboratories, Sheffield.

*Private communication.

Fig. 1—Heavily attacked 25/20 Cr–Ni steel superheater support encrusted with sodium sulphate

an earlier paper,[3] and show no tendency to accelerated attack from this cause up to 750° C, even at the very low air/gas ratio of 1 : 1. With Nimonic 80A there was no serious effect from gaseous sulphur compounds until above 1000° C, and it appears generally true from such laboratory tests that this type of trouble only occurs near the practical limit of temperature for useful service with low-sulphur fuels.

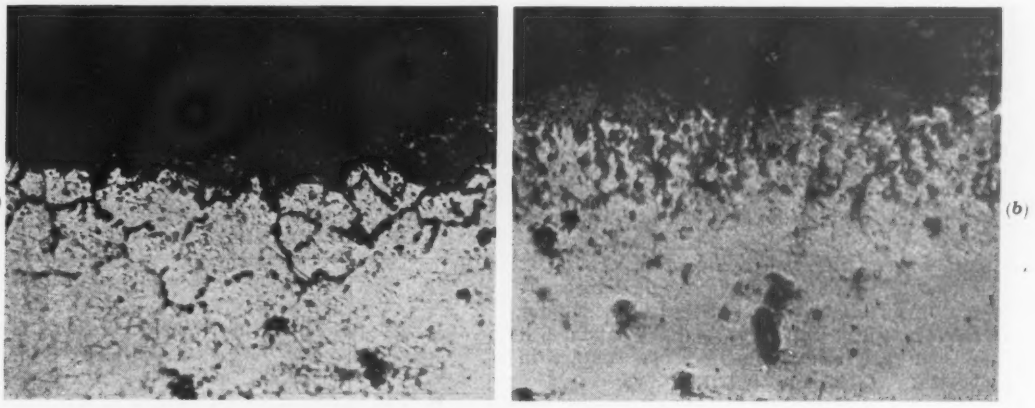

Fig. 2—Unetched sections showing local penetration of attack in air-heater tubes after 1016 h in peat trial (770° C); (*a*) 337A and (*b*) F.C.B.(T) × 500

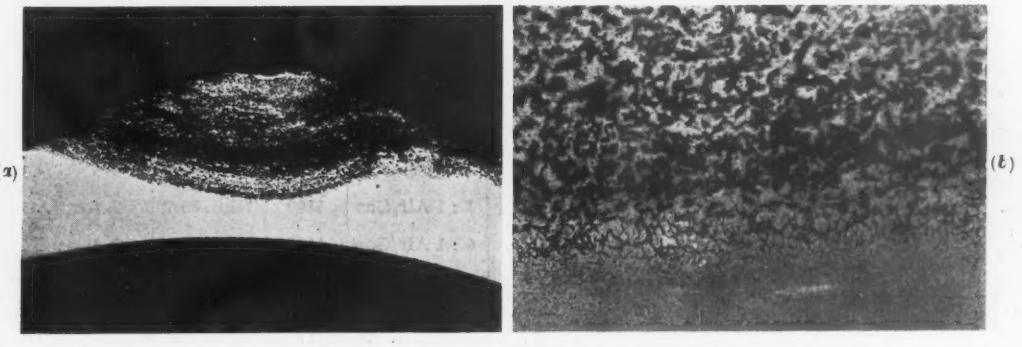

Fig. 3—Unetched section through Nimonic 80A airheater tube after 1016 h at 770° C in peat trial:(*a*) × 10, (*b*) × 100

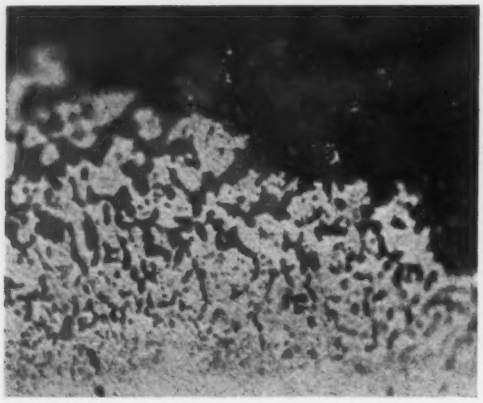

Fig. 4—Unetched section through first-stage Nimonic 80A stator blade after 20 h in open cycle peat trial × 1000

Fig. 5—Unetched section through Nimonic 80A cylinder tested 6 h in air at 750° C in contact with 99/1 Na_2SO_4–NaCl mixture × 500

On the other hand, the characteristics of the attack found in practice, including sulphur pick-up, were readily imitated by testing in contact with sodium sulphate containing a small amount of sodium chloride, even when the test was carried out in air. For example, a Nimonic 80A cylinder tested for 6 h in air at 750° C while half immersed in sodium sulphate containing 1% sodium chloride, showed very localized severe attack of the type found in service samples (Fig. 5). Because of the localized nature of the attack, surface millings contained a high proportion of relatively unattacked material, but even so, a sulphur content of 0·048% was obtained for the outer few thousandths of an inch, compared with an initial 0·003%. Evidently reduction of alkali sulphate to metal sulphide is possible under such conditions.

In view of the marine test results, it was of interest to check whether sodium-sulphate/vanadium-pentoxide mixtures were also liable to cause such a sulphur penetration attack. Tests were carried out for 6 h at 750° C in air, using Nimonic 80A samples in contact with:

(i) Vanadium pentoxide
(ii) Synthetic-oil ash mixture used in earlier tests[3]
(iii) The extremely corrosive 90/10 V_2O_5–Na_2SO_4 mixture.

In no case was anything found resembling the service sulphide type of attack.†

MAIN SERIES OF TESTS

These tests were almost entirely on cylinders of ½ in. dia. × 1⅛ in. long, cut from bar and emeried to 120 emery finish before test. Descaling after test was carried out by cathodic treatment in molten caustic soda, details of which will be found in an appendix. Results are given as loss of weight in Tables II to VII.

Table II covers the main series at 750° C dealing with variations in salt mixtures in relation to three materials: steels 337, F.C.B.(T), and Nimonic 80A.

†Later tests in a 90/10 Na_2SO_4–V_2O_5 mixture have given sulphur absorption with some visible penetration of a rather similar type to that in the sulphide–chloride mixture.

The mixtures were made up by grinding the constituents in a mortar. The test cylinders were half-immersed horizontally, using 12 g of mixture in 3·6-cm dia. silica capsules. The tests were carried out in pairs in 30 in. × 2 in. internal dia. electric tubular furnaces, open to the air at one end. Various combinations of sodium and calcium salts were included and a check was made with a 99/1 K_2SO_4–NaCl mixture for similarity to the double sodium salt mixture. A test in the 99/1 sodium salt mixture using a 6 : 1 air/gas atmosphere gave similar results to the plain air tests, with loss factors of 13, 12, and 26 respectively for the three materials.

Table I

EFFECT OF H_2S ON SCALING IN 1 : 1 AND 6 : 1 AIR/GAS COMBUSTION PRODUCTS

Test Atmosphere*	Temp., °C	Material	Loss, mg/cm², in 2 cycles of 6 h (mean of 2 samples)	
			Without H_2S	With H_2S
1 : 1 Air/Gas	750	337	...	0·2
		F.C.B.(T)	...	0·1
		Nim. 80A	...	0·1
1 : 1 Air/Gas	850	337	...	32
		F.C.B.(T)	...	1·9
		Nim. 80A	...	0·3
1 : 1 Air/Gas	1000	337	0·4	32
		F.C.B.(T)	0·4	55
		Nim. 80A	0·9	1·6
6 : 1 Air/Gas	1000	337	1·0	1·4
		F.C.B.(T)	0·7	2·9
		Nim. 80A	0·9	1·3
1 : 1 Air/Gas	1100	Nim. 80A	2·1	15
6 : 1 Air/Gas	1100	Nim. 80A	2·3	2·3

* Stoichiometric combustion requires a 4 : 1 air/gas ratio. Here the 6 : 1 air/gas mixture was burnt in the normal manner, but the 1 : 1 mixture was burnt catalytically over broken silica at the test temperature. The H_2S addition was equivalent to 0·5% SO_2 in the combustion products of the 6 : 1 mixture—crude coke-oven gas or a heavy oil with 4-5% sulphur would give about 0·2% SO_2.

Table II

EFFECT OF VARIOUS SALT MIXTURES AT 750° C IN AIR (6 h)

Test	Contact Mixture	Loss after NaOH Descaling, mg/cm²			Remarks
		337	F.C.B.(T)	Nim. 80A	
a	...	1·4	0·9	0·4	
b	100% Na_2SO_4	1·7	0·9	0·7	
c	99% Na_2SO_4, 1% NaCl	11, 12, 4	14, 13, 12	34, 34, 53	Localized 'crater' attack on immersed Nimonic
d	90% Na_2SO_4, 10% NaCl	101, 103, 23	5, 6, 3	295, 452, 497	Very severe local attack on Nimonic, with formation of eutectic globules; salt mixture fused
e	75% Na_2SO_4, 25% NaCl	131	5	169	" " " "
f	50% Na_2SO_4, 50% NaCl	99, 10, 111	3, 4, 3	91, 57, 49	" " " "
g	100% NaCl	37, 31	28, 43	16, 19	Fairly uniform attack, even on Nimonic
h	99% K_2SO_4, 1% NaCl	16	13	31	Localized 'crater' attack on immersed Nimonic
i	100% $CaSO_4$	0·8	0·8	0·3	
j	99% $CaSO_4$, 1% NaCl	32, 44	36, 49	1·0, 1·3	
k	99% $CaSO_4$, 1% $CaCl_2$	33	10, 14	1·3, 1·6	
l	90% $CaSO_4$, 10% NaCl	53, 47	44, 45	5, 4	Scaling of the steels in these mixtures was usually rather patchy, with flaking on cooling, often leaving the samples practically scale-free on brushing
m	90% $CaSO_4$, 10% $CaCl_2$	77	47	2, 2	
n	90/10 $CaSO_4$–Na_2SO_4 with 1% NaCl	8, 19	8, 7	0·6, 0·6	
o	50/50 $CaSO_4$–Na_2SO_4 with 1% NaCl	19	10	1·0	

Published[4] freezing points are 884° C for Na_2SO_4 and 779° C for NaCl, with figures of 750° C and 625° C respectively for 72/28 Na_2SO_4–NaCl mixture and the 45/55 eutectic mixture. Check tests on the mixtures used in the present tests gave 885° C for the Na_2SO_4, 875° C for the 99/1 Na_2SO_4–NaCl, 785° C for the 90/10 mixture, and 695° C for the 50/50 mixture. In the 750° C tests of Table II the 90/10 mixture appeared to fuse considerably, but the 99/1 mixture sintered without extensive melting. Since in service the constituents of salt mixtures may pass together through hotter zones before final deposition on the metal surface, the effect of fusion by preheating to 1000° C was checked for the 99/1 and 90/10 sodium salt mixtures; afterwards they were ground before testing. This was also tried for the corresponding 99/1 calcium salt mixture, although in this case no indication of fusion was anticipated or found. The results of tests are given in Table III, which also includes tests designed to check the possibility of complications from the use of silica capsules. Comparative tests were carried out in platinum.

The influence of temperature was studied over the range 600–750° C, results being given in Table IV.

From Table I it will be noted that much variability sometimes occurred, particularly in the response of 337 steel. This included the molten 50/50 Na_2SO_4–NaCl mixture with which any influence of imperfect mixing would be minimized, and further tests were carried out in this mixture. These included the effect of position in the furnace and air flow, details and results being given in Table V. The furnace temperature distribution over the test zone was within $\pm$ 5° C, whilst the test samples were all cut from a single short length of bar. In all these tests it was seen that strong alkalinity developed in the salt mixture when attack was heavy, but only slight alkalinity with light attack.

Table III

TESTS IN AIR AT 750° C FOR 6 h

To check the effect of preheating mixtures to 1000° C and of variation in the material of the test vessels

Contact Mixture	Preheating of Mixture	Test Vessel	Loss after NaOH Descaling, mg/cm²		
			337	F.C.B.(T)	Nim. 80A
99/1 Na_2SO_4–NaCl	None	Silica	10, 10	7, 8	24, 33
99/1 Na_2SO_4–NaCl	None	Platinum	7, 9	6, 7	37, 12
99/1 Na_2SO_4–NaCl	1000° C in platinum	Silica	7, 6	9, 5	9, 0·6
99/1 Na_2SO_4–NaCl	1000° C in platinum	Platinum	8, 8	8, 8	0·4, 10
90/10 Na_2SO_4–NaCl	None	Silica	103, 23	3, 5	497, 213
90/10 Na_2SO_4–NaCl	1000° C in platinum	Platinum	56, 58	6, 5	302, 538
99/1 $CaSO_4$–NaCl	None	Silica	32, 44	36, 49	1·0, 1·3
99/1 $CaSO_4$–NaCl	1000° C in platinum	Platinum	23, 19	23, 17	0·9, 0·9

Table IV

INFLUENCE OF TEMPERATURE OF ATTACK BY SULPHATE–CHLORIDE MIXTURES
(6-h tests)

Contact Mixture	Temp., °C	Loss after NaOH Descaling, mg/cm²		
		337	F.C.B.(T)	Nim. 80A
None	600	0·1	0·4	<0·1
	650	0·2	0·3	<0·1
	700	1·3	0·7	0·1
	750	1·4	0·9	0·4
99% Na_2SO_4–1% NaCl	600	0·5	1·1	0·2
	650	4	4	0·3
	700	6	5	2
	750	9	14	41
90% Na_2SO_4–10% NaCl	600	2	1·4	1·9
	650	3	3	1·3
	700	7	4	32
	750	76	5	415
50% Na_2SO_4–50% NaCl	650	1·3	1·3	1·6
	750	70	3	66
90% $CaSO_4$–10% $CaCl_2$	600	4	2	0·3
	650	2	5	0·4
	700	62	15	0·8
	750	77	47	2

The 750° C results are averages from Table I

Table V

VARIABILITY IN BREAKDOWN OF 337 STEEL OVER 700–800° C IN 6-h TESTS WITH 50/50 Na_2SO_4–NaCl

Temp., °C	Loss after NaOH Descaling, mg/cm²			
	1st Test		2nd Test	
	Static Air			
	(*a*)	(*b*)	(*a*)	(*b*)
700	2	2	3	2
750	224	5	272	198
800	426	359	479	8*
	(*a*) Nearer closed end of furnace, (*b*) nearer open end of furnace			
	Air at 18 ft³/h			
	(*c*)	(*d*)	(*c*)	(*d*)
700	2	2	2	3
750	142	3	131	151
800	522	547	342	425
	(*c*) Nearer entry, (*d*) nearer exit			

* After a further test this sample lost 492 mg/cm²

Effect of metal composition was studied, using two 90/10 mixtures, Na_2SO_4–NaCl and $CaSO_4$–$CaCl_2$, reference tests being carried out with no mixture present. All the tests were for 6 h at 750° C in air, and details and results are given in Table VI. The materials were tested in the form of the standard cylinders except for nickel and chromium. The nickel was 1-cm dia. 'shot' (3·5 cm² surface area), and for the chromium, broken cast lumps were used (surface area about 6 cm²). An interesting feature was the presence of soluble hexavalent chromium in the oxidation products from all the chromium-bearing materials tested in the calcium-salt mixture, but not with the sodium-salt mixture.

In Table VII results are given for a series of tests designed to explore the effect of a sample of actual peat ash with and without additions of Na_2SO_4–NaCl mixtures or graphite. The latter was included to check how far unburnt carbon might be effective in accelerating attack. The tests with graphite included the use of a high-sulphur combusted gas atmosphere. The bulk sample of ash used had the following percentage composition:

Ignition loss (H_2O, CO_2, C)	13·52
SiO_2	28·61
Al_2O_3	9·79
Fe_2O_3	6·91
TiO_2	0·62
CaO	12·42
MgO	14·58
K_2O	1·26
Na_2O	2·26
SO_3	9·22
Cl	0·10

This compares with an earlier sample obtained during the air heater tests, which gave:

K_2O	0·74
Na_2O	1·77
SO_3	9·60
Cl	0·10

and with a deposit from one of the tubes which contained 2·1% water-soluble Na_2O (essentially sulphate) associated with 0·1% Cl. In both these cases 5% Na_2SO_4 with about 3% NaCl impurity is possible.

Table VI

EFFECT OF 90/10 SULPHATE–CHLORIDE MIXTURES ON VARIOUS MATERIALS
6-h Tests in static air at 750° C; losses after NaOH descaling, mg/cm²

Material	C	Si	Mn	Cr	Ni	Co	Mo	Cu	Ti	Nb	Al	No Mixture	90/10 Na_2SO_4–NaCl	90/10 $CaSO_4$–$CaCl_2$
Chromium	...	...	...	...	...	...	...	...	...	...	...	0·7	1, 5	10, 12
Nickel	...	...	...	...	...	...	...	...	...	...	...	2	7, 6	10, 12
Mild steel	0·16	0·27	0·58	0·06	0·11	...	...	...	...	...	...	49	143	112
F.I.20	0·06	0·28	0·75	20·62	0·20	...	...	...	...	...	...	0·4	9, 2	118, 152
Pyrista	0·07	1·30	1·38	28·48	1·79	...	...	...	...	...	...	0·3	6, 6	104, 129
Immaculate 5	0·11	1·56	0·93	23·48	21·90	...	...	...	...	...	...	0·2	3, 5	95, 95
25% Ni steel	0·30	0·20	1·04	0·01	25·82	...	...	...	...	...	...	11	85, 99	60, 64
Inconel	0·08	0·30	0·28	13·37	78·18	...	...	...	...	...	...	0·2	184, 355	2, 3
Nimonic 80A	0·05	0·53	0·05	20·22	74·30	...	...	...	2·08	...	0·67	0·4	295, 452	2, 2
F.C.B.(T)	0·08	0·60	1·51	17·88	12·43	...	...	...	...	0·80	...	0·9	5, 6, 3	47
337	0·22	0·73	0·69	16·40	17·50	7·34	2·73	2·80	0·86	...	...	1	101, 16, 103	77

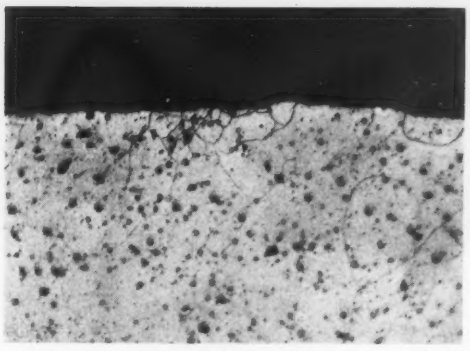

Fig. 6—Etched section from 337A cylinder tested 6 h in air at 750° C in contact with 99/1 Na_2SO_4–NaCl mixture × 100

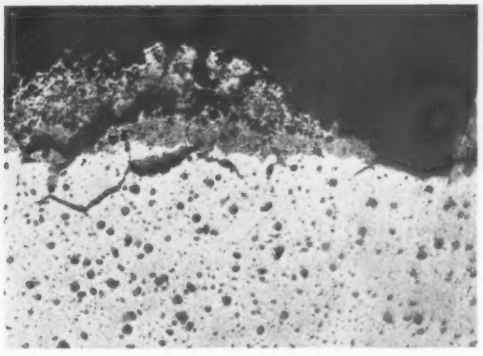

Fig. 8—Unetched section of 337A cylinder tested 6 h in air at 750° C in contact with 90/10 $CaSO_4$–$CaCl_2$ × 100

Subsidiary tests were made with Nimonic 80A in 50/50 sodium-sulphate–graphite mixture. In 6 h at 750° C losses of 0·2 and 0·5 mg/cm² respectively were obtained for tests in 6/1 combusted air-gas and static air (closed furnace).

EXAMINATION FOR STRUCTURAL FEATURES OF ATTACK

Micrographic examination was made on sections cut from test pieces representing all the main test conditions. A selection of these has been made to illustrate points of special interest.

Figures 6–8 relate to a form of intercrystalline penetration found with 337 steel in the sulphate–chloride mixtures at 750° C. This had already been noted in the earlier work[3] with impure sodium sulphate. With the present samples it occurred as an irregular patchy effect, much more evident in lightly attacked than in heavily attacked samples. It appeared to be confined to formation of an intercrystalline phase in the 99/1 mixture, but intergranular separation occurred with the 90/10 and 50/50 mixtures, including the 90/10 calcium salts. Depth of penetration beyond the general attack was relatively small, and the bend of a service-tube wall (Fig. 2) round $4\frac{1}{2}t$ produced no crack penetration to suggest grain boundary embrittlement.

A similar effect was found with F.C.B.(T) (Fig. 9), bending round $3\frac{1}{2}t$ producing no noticeable opening up of the material along the affected boundaries.

Typical sulphide penetration zones in Nimonic 80A have already been noted in Figs. 3–5. Similar effects, but showing great variation in detailed layout, have been found throughout test series as Figs. 10, 12, and 13 show. A very interesting feature was the growth of small magnetic 'spheres' of metallic appearance, but in fact consisting of Ni–NiS–NiO eutectic mixtures. A very small example is seen embedded in the sulphide-affected zone in Fig. 10, and an etched

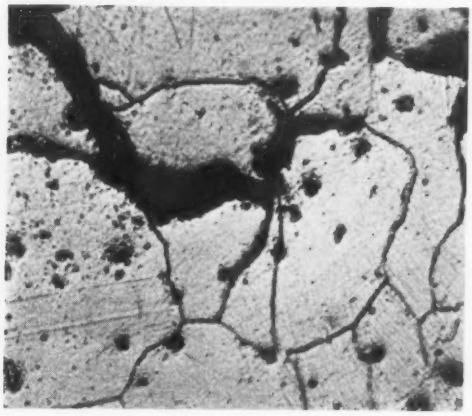

Fig. 7—Etched section of 337A after 6 h in air at 750° C in contact with 50/50 Na_2SO_4–NaCl mixture (90/10 mixture similar) × 500

Table VII

TESTS WITH PEAT-ASH MIXTURES AT 750° C FOR 6 h

Atmosphere	Contact Mixture	Loss after NaOH Descaling, mg cm²		
		337	F.C.B.(T)	Nim. 80A
Static Air	...	1·4	0·9	0·4
	Peat ash	4	2	0·4
	Peat ash with 1% NaCl	11	4	2
	Peat ash with 5% of 97/3 Na_2SO_4–NaCl	9	3	0·4
	Peat ash with 10% of 97/3 Na_2SO_4–NaCl	9	3	1·2
	Peat ash with 20% of 97/3 Na_2SO_4–NaCl	28, 24	12, 15	12, 8
	Peat ash with 50% of 97/3 Na_2SO_4–NaCl	33	19	104
6 : 1 Air/Gas	...	1·5	0·8	0·4
	Peat ash	3	1·4	0·2
	50/50 Peat-ash/graphite	4	3	0·1
6 : 1 Air/Gas with H_2S	...	1·7	0·7	0·6
	Peat ash	4	2	0·6
	50/50 Peat-ash/graphite	5	4	0·3

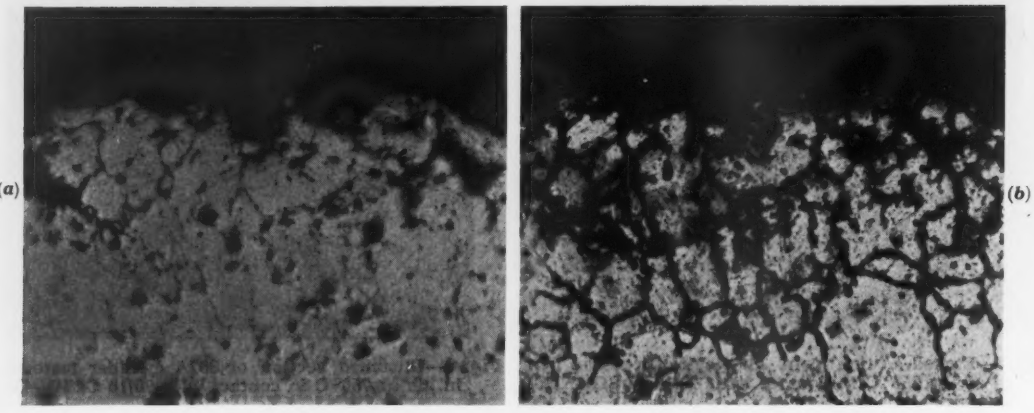

Fig. 9—Section of F.C.B.(T) cylinder tested 6 h in air at 750 C in contact with 99/1 Na_2SO_4–NaCl mixture: (*a*) unetched × 500, (*b*) etched × 500

section of a typical larger specimen from a test in 50/50 Na_2SO_4–NaCl is shown in Fig. 11. Analysis of a collection of these globules from a test in the 50/50 mixture gave:

sulphur 15·2%; nickel 78·9%; chromium < 0·1%.

Assuming the remaining 6% to be substantially oxygen, and combination to NiS and NiO, a composition of 40–45% NiS with up to 30% of nickel and of nickel oxide is indicated.

A bend test on similar lines to those illustrated for the two steels produced no cracking from previous attack in 90/10 Na_2SO_4–NaCl mixture.

CONSIDERATION OF RESULTS

The results in Table I show that no trouble from gaseous sulphur compounds is to be expected until approaching the normal upper limit of useful oxidation-resistance of the materials tested. Such compounds, therefore, cannot be held responsible for the severe service attacks which led to the present work. These service failures are, on the other hand, closely simulated by the sulphate–chloride attack of the tests detailed in Table II. Considering the results from this series, especially interesting features are:

(i) The absence of any appreciable effect from the plain Na_2SO_4 or $CaSO_4$

(ii) The severity of the effect from the small 1% addition of chloride

(iii) The contrast between F.C.B.(T) and Nimonic 80A is the much more intense effect on the latter in sodium salt mixtures compared with the more serious effect on the steel in the calcium salt mixtures, whilst 337 suffered severely in both types of mixture. The tendency to severely localized attack with Nimonic 80A in the sodium salt mixture is a point of some practical importance, as this greatly increases the rate of local penetration.

The results in Table III show that the 99/1 sodium salt mixture is less dangerous to Nimonic 80A if preheated to 1000° C, although two out of the four tests produced attack approaching that in the untreated mixture. The preheating effect was not apparent for the steels or for the other mixtures. The use of silica test vessels also appeared to be without effect.

Table IV explores the influence of temperature. With the steels an increased attack was found earlier

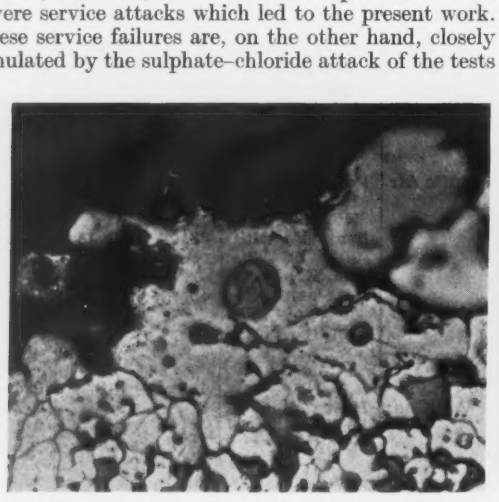

Fig. 10—Section through heavily attacked zone of Nimonic 80A cylinder after test for 6 h in air at 750° C in contact with 99/1 Na_2SO_4–NaCl mixture; outer zone after etching × 1000

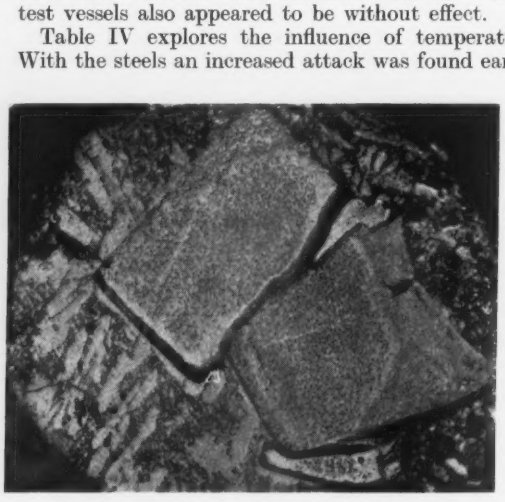

Fig. 11—Etched section through eutectic globule from Nimonic 80A cylinder tested for 6 h in air at 750° C in contact with 50/50 Na_2SO_4–NaCl mixture × 250

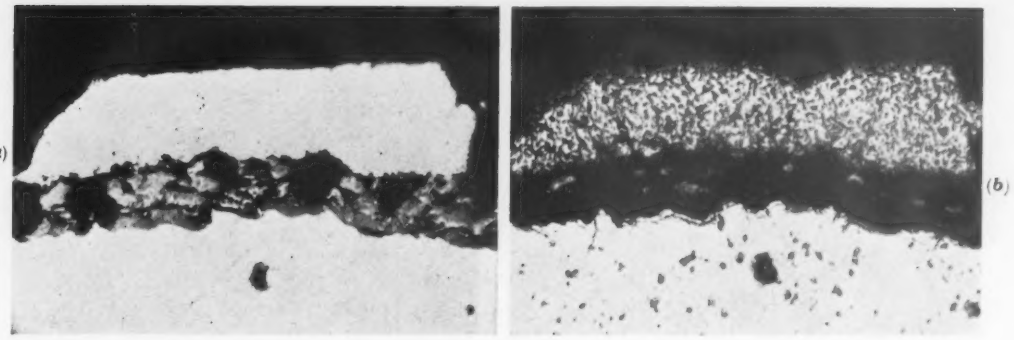

Fig. 12—Sections of Nimonic 80A cylinders tested for 6 h in air at 750° C in contact with 99/1 Na_2SO_4–NaCl mixture: (*a*) and (*b*) unetched and etched views showing wide band of dark constituent × 500

than for Nimonic 80A, being apparent at 600–650° C, but catastrophic attack did not begin with either until about 700° C.

Variability in the breakdown of 337 steel, within the 6-h test appears from Table V to be a ' statistical ' matter, not obviously related to furnace atmosphere or to position in the test furnace. In these tests there was no indication of heavy attack at 700° C, but the expectation of severe attack is indicated to be rather higher at 800° than at 750° C. The extent of attack at the higher temperature was also greater, by a factor of two or three.

The effects of composition brought out by the results in Table VI are in some cases curious. Thus, chromium greatly improved the resistance of iron to the sodium salt mixtures, but the two high nickel–chromium alloys showed very greatly reduced resistance compared with the plain chromium and nickel figures. In the calcium salt mixture, alloying iron with chromium or chromium and nickel produced no great improvement, but in this case the high nickel–chromium alloys had a good resistance, superior to that of either element alone. Evidently service behaviour is likely to be greatly affected by relative availability and proportions of alkali to calcium salts in the deposits.

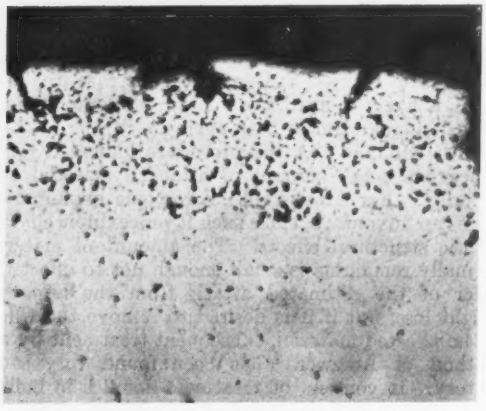

Fig. 13—Unetched section through Nimonic 80A cylinder after 6 h in air at 750° C in contact with 100% NaCl × 500

Effects related to availability can also arise through the ' blanketing ' action of the silica and less fusible alkaline-earth sulphates, which will predominate in the service ash deposits. If these remain undisturbed, they will tend to restrict severely access of fresh supplies of chloride and alkali salts, so that the severity of the attack may become and remain low, even in the presence of appreciable chloride-contaminated alkali sulphate. This is illustrated by the considerable addition of the corrosive alkali sulphate–chloride mixture required to produce severe attack by the bulk peat ash (Table VI). The effect has also been found in service. For example, with high nickel, high chromium alloy gas-turbine blades, regional differences have been found associated with the degree of erosion. Where erosion was virtually absent, and a calcium-sulphate-rich deposit built up thickly, only slight attack of the type of Fig. 4 developed. Zones intermediate between this and severely eroded deposit-free zones showed evidence of much greater sulphide attack, presumably because the combined effects of erosion and deposition ensured more continuous availability of dangerous alkali sulphate–chloride mixtures.

Table VII also includes the tests with peat-ash/graphite mixtures and indicates that the effect of the presence of unburnt carbon in the ash is likely to be much less than that of the availability of chloride-contaminated sulphates. No deleterious action was found with the Nimonic 80A in the 50/50 ash–graphite mixture.

Structurally, the most important practical feature is the absence of any notable embrittlement from the various penetrations and precipitates associated with attack by the various mixtures used. It is possible that some effect will be found in fatigue, but no failure attributable to such action has been encountered so far by the author.

CONCLUSIONS

Severe attack on heat-resisting steels and high nickel–chromium alloys under service conditions in both peat and oil-burning equipment has been found to be associated with much sulphur absorption. Laboratory tests have indicated that such attack is not related to direct sulphur absorption from gaseous combustion products, but does occur through contact

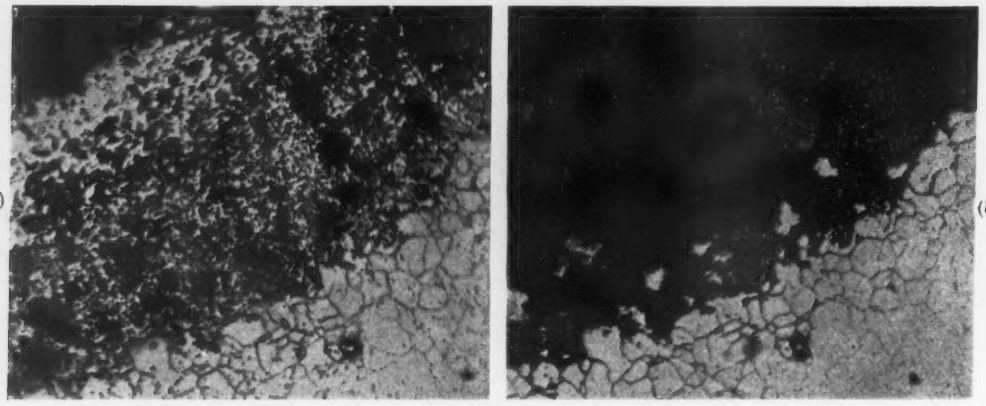

Fig. 14—Etched section through cylinder of Nimonic 80A after 6 h in air at 750° C in contact with 99/1 Na_2SO_4–NaCl mixture: (*a*) after electrolytic descaling in molten NaOH × 500, (*b*) after subsequent Amgwerd treatment × 500

with chloride-contaminated alkali and alkaline-earth sulphates, as little as 1% chloride inducing a high rate of attack at 700–750° C.

With Nimonic 80A, alkali–salt mixtures are particularly dangerous, but with F.C.B.(T) steel calcium salts had a more serious effect, 337 steel being severely attacked by both types. More general effects of composition are complicated. Chromium has a protective action on steels against alkali salts, but little action against calcium salts. Nickel and chromium together in high nickel–chromium alloys suffer a great drop in resistance to alkali–salt mixtures compared with their individual resistance, but their resistance to calcium salts is improved.

The sulphate–chloride type of attack increases rapidly from 650° to 750° C. The probability of a breakdown in a limited time also increases with temperature in the 700–800° C range

Availability of the dangerous salt mixtures at the metal surface is a feature of practical importance. There is indication that the large amount of alkaline-earth sulphates and inert materials present in the peat ash can have a very useful ' blanketing ' effect against the arrival of fresh supplies of chloride and alkali salts in the absence of erosion.

The presence of unburnt carbon in peat ash at 750° C gave some increase in attack on the steels, but not with Nimonic 80A. It does not appear to be a probable cause of severe attack in service.

Some structurally selective penetration of attack occurred in the salt mixtures, but no serious embrittlement was found associated with this, even in severely affected samples.

Acknowledgments

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APPENDIX

Technique for Descaling Samples after Test

Cathodic treatment in molten caustic soda at 400–420° C has been found very satisfactory for removing the bulk oxide from service and test samples. A current density of about $\frac{1}{3}$ A/cm² (2–2$\frac{1}{2}$ A/in²) is effective, giving a ' blank ' loss of the order of 0·01 mg/cm² in 10 min. In most cases a single treatment for 5 min, followed by water quenching is sufficient.

It is unnecessary in the author's experience to use higher temperatures, and any great increase to above 500° C is likely to introduce undesirable complications into the blanks and in bath ageing.

With Nimonic 80A the procedure indicated tends to leave a small amount of attacked material still adhering. For subsequent microscopic examination this is an advantage, since such layers exhibit characteristic structural effects. The amount of material normally remaining is small enough not to affect the order of the estimated attack from the apparent weight loss, but if it is desired to remove the whole of the affected material, subsequent treatment by the method of Amgwerd[5] has been found very satisfactory. It consists of treatment for 2 h in boiling 30% Na_2CO_3–2% $K_2Mn_2O_8$, followed by 2 min in 20% by volume nitric acid containing 1% of 20-vol. hydrogen peroxide. An idea of the magnitude of the

effects is given by the following figures for specimens tested in sulphate–chloride mixtures at 750° C:

Test Mixture	Original Loss, mg/cm², from NaOH descaling	Further Loss in Amgwerd Treatment
99/1 Na_2SO_4–NaCl	53	4
90/10 Na_2SO_3–NaCl	497	6
100 NaCl	19	1
99/1 $CaSO_4$–NaCl	1·0	0·6

Micrographic examination of treated samples has shown no effect by this second treatment on the sound metal. This is illustrated by views of the same area before and after treatment for a Nimonic 80A cylinder from 99/1 Na_2SO_4–NaCl (Fig. 14).

Dephosphorization of Alloys by Hydrogen

By P. C. Ghosh, B.Sc., B.Met., A.M.I.E., A.I.M., and B. Chatterjee, D.Sc.

SYNOPSIS

The loss of phosphorus on heating powdered samples of phosphor-copper, ferrophosphorus, and a cast iron in hydrogen and nitrogen, both dry and wet, has been determined. An appreciable dephosphorization has been observed under these conditions. The results seem to indicate the formation of phosphine and nitrogen phosphide in these experiments. Introduction of moisture in hydrogen increases the rate of dephosphorization in two ways, viz. through direct interaction between water and phosphorus, and by disturbing the equilibrium of the reaction between phosphorus and hydrogen through oxidation of phosphine. 1119

RECENT EXPERIMENTS by the authors[1] on prolonged heating of blocks (15 mm × 8 mm × 8 mm) of a sample of cast iron (C, 3·08%; Si, 0·88%; Mn, 0·216%; P, 0·21%; and S, 0·10%) in flowing hydrogen, both dry and wet, at 1000° C have shown that the phosphorus content of the solid samples is not diminished as a result of the treatment. This may result from either a lack of reactivity between the phosphorus of the alloy and hydrogen or from extremely low diffusivity of the reactants. The present investigation has been undertaken to obtain further information on these points. Estimations have been made of the amount of phosphorus removed, if any, from powdered samples (40 mesh, B.S.S.) of phosphor-copper (P, 14%), and ferrophosphorus (P, 26·6%), and fine drillings of a sample of cast iron (Si, 2·5%; Mn, 0·95%; P, 1·1%; and S, 1·05%) by hydrogen and nitrogen, both wet and dry. The use of powdered samples and drillings was expected to facilitate the diffusion of phosphorus to the surface of the specimens.

EXPERIMENTAL PROCEDURE

The experimental set-up used in the present investigation was the same as that described in the previous publication[1]: 5·0 g of powdered samples and drillings contained in porcelain boats were placed inside a quartz tube through which purified gases were passed. The tube was then heated in an electric furnace at the desired temperature for the desired length of time. The gas flow through the tube was maintained at the rate of 240 cm³/min, using flow meters. The moisture content of the wet gases was fixed at 1·2 vol.-% by keeping the gas flow and the water-bath temperature constant. In the experiments with wet gases, the gases were bubbled through the water bath before being led to the quartz tube. The phosphorus contents of the samples were determined by the usual method, i.e. precipitation of the phosphorus as ammonium phosphomolybdate followed by titration with standard alkali.

RESULTS

Phosphor-Copper

The results so far obtained on the dephosphorization of phosphor-copper at different temperatures by passing dry and wet gases over the specimens for varying periods of time have been summarized in Table I. Thermodynamic considerations indicate that with the gas compositions used, no oxidation of copper should take place. This fact has to be considered in judging the data presented.

Table I

DEPHOSPHORIZATION OF PHOSPHOR-COPPER (P, 14%) ON TREATMENT WITH GASES

Treatment		Percentage Reduction of Phosphorus on Treatment with			
Temp., °C	Time, h	Dry H_2	Wet H_2	Dry N_2	Wet N_2
500	8	0·71	10·00		
800	8	1·43	15·00		
900	2	0·71	8·59		
1000	1	3·57	15·00	2·85	12·00
1000	2	5·00	21·43	4·28	15·00
1000	4	17·14	31·43		
1000	8	30·00	44·30	9·28	30·71

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The data presented in Table I show that except at 1000° C dry gases could effect very little dephosphorization at all temperatures and that the rate of this process increased with rise of temperature. Dephosphorization with hydrogen was found to be slightly more than that by nitrogen, the relative effects of the two gases being clearly illustrated in experiments in which the specimens were heated at 1000° C for 8 h.

In the interaction between dry hydrogen and phosphor-copper, phosphorus is removed according to the reaction:

$$P + \tfrac{3}{2}H_2 = PH_3 \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (1)$$

That phosphine was formed in this reaction is supported by the fact that the exit gas in experiments where dry hydrogen was used was found to give indications of phosphine when tested with silver nitrate paper and with solutions of silver nitrate, copper sulphate, mercuric chloride, etc. In addition the smell of phosphine was quite perceptible in the exit gas. When using dry nitrogen, the exit gas, both before and after filtration through glass wool, did not respond to any of the tests mentioned. Also, the gas coming out of the quartz tube when using both dry hydrogen and dry nitrogen was found to give a neutral reaction when tested with litmus paper. Details of the chemical examination of exit gases for experiments with phosphor-copper and ferrophosphorus are summarized in Table II.

In the case of treatment of the sample alloy in pure dry nitrogen, there is the possibility, as indicated later in the paper, of formation of nitride of phosphorus. The fact that hydrogen is more effective in removing phosphorus from the alloys probably indicates that phosphine is more stable than nitrogen phosphide at 1000° C.

When moisture is present in the gas, the main dephosphorization when using nitrogen seems to take place according to the reactions:

Table II

CHEMICAL EXAMINATION OF THE EXIT GAS

Reagents Used	On Treatment of the Alloys with							
	Dry Hydrogen		Wet Hydrogen		Dry Nitrogen		Wet Nitrogen	
	Unfiltered	Filtered	Unfiltered	Filtered	Unfiltered	Filtered	Unfiltered	Filtered
Silver nitrate (solid)	Yellow gradually turning black	Yellow gradually turning black	Rapid change to yellowish black	No change	No change	No change	Yellow to black	No change
Silver nitrate paper (dry)	Yellow to black; slow reaction	Yellow to black; slow reaction	Yellow to black	No change	No change	No change	Yellow to black	No change
Silver nitrate (solution)	Black precipitate	Black precipitate	Yellow precipitate turning black	No change	No change	No change	Yellow precipitate turning black	No change
Copper sulphate (solution)	Grey precipitate	Grey precipitate	Greenish blue to black	No change	No change	No change	...	...
Mercuric chloride (solution)	White precipitate	White precipitate	White precipitate	No change	No change	No change	White precipitate	No change
Cadmium chloride solution with acetic acid	No change	No change	No change	No change	No change	No change	No change	No change
Albumen solution	No change	No change	No change	No change	No change	No change	No change	No change
Ammonium molybdate solution with nitric acid	Light yellowish; slow reaction	Light yellowish; slow reaction	Yellow precipitate	No change	...	...	...	...
Barium chloride solution	No change	No change	No change	No change	No change	No change	No change	No change
Litmus paper	No change	No change	Acidic	No change	No change	No change	Acidic	No change

Table III

DEPHOSPHORIZATION OF FERROPHOSPHORUS (P, 26·6%) AND CAST IRON (P, 1·1%) ON TREATMENT WITH GASES AT 1000° C

System	Time of Treatment, h	Percentage Reduction of P on Treatment with			(% Fe)/(% P) in Sample on Treatment with		
		Dry H_2	Wet H_2	Dry N_2	Dry H_2	Wet H_2	Dry N_2
Ferrophosphorus	0				2·62	2·62	2·62
,,	4	10·20	22·60	4·90	3·01	3·60	2·80
,,	8	22·90	34·60	19·20	3·62	4·32	3·41
Cast iron	0				83·50	83·50	
,, ,,	8	18·00	27·30		101·90	116·30	

$$2P + 5H_2O = P_2O_5 + 5H_2 \quad \ldots\ldots(2)$$
$$2P + 3H_2O = P_2O_3 + 3H_2 \quad \ldots\ldots(3)$$

Phosphorus pentoxide and phosphorus trioxide in turn react with water, forming phosphoric and phosphorous acids:

$$P_2O_5 + 3H_2O = 2H_3PO_4 \quad \ldots\ldots(4)$$
$$P_2O_3 + 3H_2O = 2H_3PO_3 \quad \ldots\ldots(5)$$

This assumption is supported by the observation (Table II) that the exit gas in this case was found to be acidic to litmus and to respond to the usual tests for orthophosphoric and phosphorous acids (i.e. with solid silver nitrate and with solutions of silver nitrate, barium chloride, and ammonium molybdate in the presence of nitric acid). It was also found that when the treatment gases had been filtered through water and glass wool the exit gas did not respond to any of the above tests nor to any for phosphine. The formation of ortho-phosphoric and phosphorous acids in the interaction between phosphor-copper and moist nitrogen is thus indicated.

In discussing the mechanism of dephosphorization of phosphor-copper by wet hydrogen, the following facts need careful consideration: (i) dry hydrogen removes more phosphorus than that removed by dry nitrogen; (ii) when dry hydrogen is used, the exit gas is found to contain phosphine; (iii) PH_3 is absent in exit gas when moist hydrogen is used; (iv) moist hydrogen removes more phosphorus compared with dry hydrogen; (v) the exit gas is found to be neutral when dry hydrogen is used; and (vi) orthophosphoric and phosphorous acids are present in the exit gas when moist hydrogen is used. Taking all these observations into consideration, it seems that the following reactions take place in the interaction between the alloy and wet hydrogen at 1000° C:

(i) $\frac{1}{4}P_4 + \frac{3}{2}H_2 = PH_3 \quad \ldots\ldots(1)$
(ii) $2PH_3 + 5H_2O = P_2O_5 + 8H_2 \quad \ldots\ldots(6)$
(iii) $2PH_3 + 3H_2O = P_2O_3 + 6H_2 \quad \ldots\ldots(7)$
(iv) $\frac{1}{2}P_4 + 5H_2O = P_2O_5 + 5H_2 \quad \ldots\ldots(2)$
(v) $\frac{1}{2}P_4 + 3H_2O = P_2O_3 + 3H_2 \quad \ldots\ldots(3)$
(vi) $P_2O_5 + 3H_2O = 2H_3PO_4 \quad \ldots\ldots(4)$
(vii) $P_2O_3 + 3H_2O = 2H_3PO_3 \quad \ldots\ldots(5)$

It will be seen that moisture plays a dual role. Firstly, it directly combines with phosphorus to form phosphorus pentoxide and phosphorus trioxide, which subsequently react with water to form orthophosphoric and phosphorous acids. Secondly, water oxidizes phosphine to phosphorus pentoxide and trioxide and thus disturbs the equilibrium of the reaction (1), and as such the dephosphorization reaction between phosphorus and hydrogen is enhanced.

Ferrophosphorus and Cast Iron

The results so far obtained on the dephosphorization of powdered samples of ferrophosphorus and drillings of a specimen of cast iron are, in a general way, in agreement with those reported for phosphor-copper as shown in Table III.

It will be seen that hydrogen is more effective in removing phosphorus from the alloys than nitrogen, and that introduction of moisture enhances the rate of dephosphorization. A distinct smell of phosphine was perceptible in the exit gases only when dry hydrogen was used. Tests for phosphine as given in Table II were not performed when cast iron was used because hydrogen sulphide formed in the interaction between hydrogen and sulphur of cast iron used would react with the reagents and change the colour of the precipitate.

If it is assumed that nitrogen is an inert gas and that the observed loss of phosphorus when using dry nitrogen is due to volatilization, the minimum phosphorus vapour pressure over ferrophosphorus, as calculated from the data presented in Table III, is of the order of 10^{-4} atm. This value is very high compared with that calculated from thermodynamic data on phosphides given by Richardson.[2] It is therefore believed that phosphorus forms a nitride (P_3N_5) and, although it will be almost completely dissociated at 1000° C, its formation may be the reason for the high minimum phosphorus vapour pressure over ferrophosphorus noted in the present investigation. The ferrophosphorus used probably has the composition 'Fe_2P and X',[3] X being an alloy of unknown composition, as against Fe_3P used by Richardson. This difference in the composition of the ferrophosphorus used in this work may have some effect, even if small, on the observed high *minimum* phosphorus vapour pressure.

References

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Determination of Vanadium in Highly Alloyed Steels

By the Methods of Analysis Committee of B.I.S.R.A.

SYNOPSIS

Standard methods already accepted by the British Standards Institution for the determination of vanadium in mild steel and in ferro-vanadium have been used as a basis for a procedure suitable for application to alloy steels. Improvement in endpoint detection has been achieved by modification of the indicator solution and by the addition of hydrofluoric acid before titration. 1183

TWO METHODS* previously issued by the British Standards Institution have provided for the determination of vanadium in carbon and low-alloy steels and irons, and also in ferro-vanadium. The present paper covers the work carried out by the Highly Alloyed Steels and Ferro-Alloys Analysis Sub-Committee in applying a similar method principle to highly alloyed steels.

The Pig Iron, Carbon and Low Alloy Steels Analysis Sub-Committee has already dealt† with methods using (*a*) cupferron separation, (*b*) mercury cathode separation, (*c*) potentiometric titration, and (*d*) direct oxidation-reduction titration. After a detailed investigation a method was proposed which used an oxidation in the cold from V^{4+} to V^{5+} by potassium permanganate; after destroying the excess of this reagent the vanadium was titrated with standard ferrous ammonium sulphate solution using barium diphenylamine sulphonate as internal indicator. The final endpoint was taken by back titration with potassium dichromate. This method was satisfactory in the presence of 3% Co, 3% Cr, 3% W on a 2·5-g sample basis.

The Highly Alloyed Steels and Ferro-Alloys Analysis Sub-Committee has also investigated‡ a similar method principle for application to ferro-vanadium samples with the following observations, which are pertinent to the present report:

(*a*) For oxidation in the cold by potassium permanganate to the quinquevalent state, a slow reaction, it is necessary to have a small but definite excess of potassium permanganate

(*b*) The titration reaction and endpoint sensitivity is improved by the presence of fluorides or phosphates

(*c*) Opinion is divided as to the advisability of completing the titration on a reduction or oxidation endpoint.

DETAILS OF INVESTIGATION

Taking into account the investigation already made it was decided to apply the same procedure as in British Standard 1121: Pt. 27, but to omit the phosphoric acid from the solvent mixture. In the presence of tungsten, hydrofluoric acid was added before oxidation with nitric acid, in order to complex the tungsten. In all tests, phosphoric acid was added before the titration to increase endpoint sensitivity.

The endpoint using internal indicator was taken at the oxidation potential by back-titrating with N/40 potassium dichromate solution. Results of the first trials are given in Table I.

Sample MGS/177 with 13% chromium gave a very dark green background colour which tended to make endpoint detection very difficult, although the endpoint selected coincided with that obtained by potentiometric titration.

Where phosphoric acid had been added after complexing tungsten with hydrofluoric acid, there was a tendency to a sluggish endpoint which was somewhat improved by omitting the phosphoric addition. Although the results were very satisfactory there was some controversy as to their accuracy and a major criticism was that on applying this procedure to accepted standard steels the results obtained were in all cases higher than the certificate value. Blank corrections, which are additive, had already been made to obtain these results, but it appeared that

Table I

DETERMINATION OF V: BACK TITRATION METHOD

Analyst	Vanadium, % — Sample MGS/		
	48	176	177
1	1·54	0·53	0·34
2	1·51	0·53	0·33
3	1·51	0·54	0·33
4	1·49	0·53	0·31
5	1·54	0·55	0·33
6	1·51	0·53	0·32
7	1·50	0·52	0·34
Averages	1·52	0·54	0·33

Composition of Steels	C	Cr	Co	Mo	W
MGS/48	1·0	5	12	0·5	22
166	1·0	4	—	—	10
176	0·3	3	—	—	10
177	2·0	13	—	0·7	—
212	0·8	4	—	2	8
225	—	1·5	—	—	2
226	vanadium-free			pure iron	
228	—	65	—	—	—

* B.S. 1121: Pt. 25: 1952, "Vanadium in ferro-vanadium."
B.S. 1121: Pt. 27: 1952, "Vanadium in carbon and low alloy steels."

† *J. Iron Steel Inst.*, 1952, vol. 171, pp. 81–85.

‡ *Ibid.*, vol. 170, pp. 343–347.

Paper MG/D/235/54 of the Methods of Analysis Committee of the British Iron and Steel Research Association, received 1st July, 1955.

Table II

DETERMINATION OF V: DIRECT TITRATION METHOD

Analyst	Vanadium, % – Sample MGS			
	166	177	212	Synth. 2% V
1	4·00	0·33	2·17	1·98
2	4·02	0·33	2·15	2·01
3	4·05	0·35	2·19	2·00
4	3·96	0·33	2·14	1·98
5	4·04	0·31	2·17	1·97
6	3·98	0·32	2·12	—
7	4·12	0·32	2·17	—
8	4·05	0·33	2·17	—
9	4·04	0·34	2·15	1·97
10	4·05	0·34	2·18	2·02
Averages	4·04	0·33	2·16	1·99

there was some discrepancy in the method of blank assessment, as the results would have been nearer their true value without this blank correction. On the other hand, positive values were only obtained on steels of low vanadium content (0·01%) when the blank correction was added.

From these preliminary trials it was apparent that the method would give reproducible results which were slightly high. The main points to be investigated were of detail rather than of a fundamental nature.

Endpoint Determination

When trying to decide on the form of endpoint detection, i.e. whether to use a direct titration with ferrous sulphate or back-titrate with potassium dichromate, there has been a continual division of opinion. The first observation was that the back titration is rendered sluggish by the presence of phosphate ions, i.e. the endpoint is indecisive and is affected more than that obtained by direct titration.

The omission of phosphate improved the endpoint detection, but through several series of tests it was shown that the presence of fluoride was advantageous even in the absence of tungsten. The hydrofluoric acid addition has the effect of intensifying the indicator colour and the end-point obtained by direct titration is sharp and decisive. The back titration is improved but is not as sharp and is spread over about 0·2 ml of N/40 potassium dichromate, giving a difference in vanadium content which can be seen from Table III. The main advantage in the direct titration is visual and cannot be shown satisfactorily on a set of figures: e.g. Table II was obtained by direct titration and does not show any improvement over Table I as regards reproducibility. In view of the visual evidence the committee recommended that direct titration should be used in the final method.

Endpoint Detection and Blank Determination

The first experimental trials were carried out using the barium diphenylamine sulphonate indicator suggested in the previous B.S.I. methods. The blank due to using the unoxidized form of indicator was evaluated as before during the standardization of the N/40 titrating solutions. Following the first trials it was felt, however, that this method of 'blank' assessment was not a true one and it was decided that a solution 'blank' should be carried through the method procedure. This solution should then be utilized for the standardization of the titrants and so determine the amount of N/40 dichromate used in oxidation of the indicator.

This method of blank determination was used in the procedure which gave the results in Table III, and it was felt that although the blank was very small it was still the main factor in giving the high results which are apparently obtained by the direct titration. This error can be still further reduced by operating the method on a sample of vanadium-free iron and so determining the 'blank' value. During these series of tests various supplies of indicator were used and a very wide variation in the blank value from batch to batch of the reagent was noted. In view of this rather disturbing factor it was decided that the method should be given a trial using the pre-oxidized indicator on a series similar to that given in Table III.

The method gave a much improved performance, as in general a nil value for the blank was reported;

Table III

DETERMINATION OF V COMPARING BACK AND DIRECT TITRATION METHODS

Analyst	Vanadium, % — Sample MGS/									
	225		226		226 + 0·03% V		226 + 0·10% V		228	
	a	b	a	b	a	b	a	b	a	b
1	0·057	0·042	No indicator response	—	0·055	0·030	0·124	0·100	0·143	0·118
2	0·048	0·039		0·004	0·044	0·032	0·111	0·100	0·128	0·121
3	0·063	0·043		0·007	0·070	0·035	0·117	0·100	0·16	0·13
4	—	0·049		0·008	—	0·034	—	0·102	—	0·124
5	—	0·046		0·003	—	0·035	—	0·10	—	0·118
6	—	—		trace	—	0·02	—	0·09	—	0·13
7	—	0·043		—	0·050	0·038	0·12	0·10	0·16	0·15
8	—	0·053		0·006	—	0·036	—	0·099	—	0·127
9	—	0·037		0·006	0·041	0·034	0·114	0·108	—	0·106
10	—	0·049		0·010	—	0·037	—	0·097	—	0·134
11	0·07	0·050		0·010	0·050	0·032	0·12	0·10	0·14	0·12

a = direct titration *b* = back titration

at the 0·03% V level the direct titration gave results from 0·032 to 0·040 and at 0·10% V from 0·102 to 0·111, whilst the back-titration gave 0·020–0·036 for 0·03% and 0·082–0·09 for 0·10% V. From this it was recommended that the the pre-oxidized indicator in solution or solid form should be used and that the blank determination should be carried out on a sample of pure iron. Even so it is admitted that absolute accuracy cannot be obtained on samples of low vanadium content by a method which involves no preliminary separations.

Suppression of Tungsten Interference by Hydrofluoric Acid and its Effect on Endpoint Detection

As previously stated, the addition of hydrofluoric acid was found to sharpen the endpoint compared with that obtained in the presence of phosphoric acid and a few experiments were carried out with a view to measuring the difference. The optical transmission curves for the oxidized and reduced forms of indicator were measured and it was found that the colour change from violet to green could be followed at a wavelength of either 4000 or 5250Å, the latter being preferable. Two solutions, each containing about 10 mg vanadium, were titrated with 0·1N ferrous sulphate solution to within 0·2 ml of the endpoint. The titrations were then completed with 0·01N solution, the transmission being measured at 5250Å on a spectrophotometer after the addition of each small increment of titrant until the endpoint was overshot. Selected readings were as follows:

	0·01N $FeSO_4$, ml	
Transmission (5250), %	HF	H_3PO
20	1·1	0·50
30	1·2	1·40
50	1·25	1·65
70	1·3	1·72

The volumes of titrant required to cause a given change in transmittance in the two cases show clearly the superiority of the hydrofluoric acid endpoint. In particular the phosphoric acid curve showed a long ' tail ' on the oxidized side of the endpoint.

In addition to sharpening the endpoint, hydrofluoric acid also performs another function. In the earlier method the phosphate present was sufficient to suppress tungsten interference up to 5%, but hydrofluoric acid was found to be fully effective in this respect even for highly alloyed steels. A minimum addition of 10 ml is recommended as standard practice for all types of steel and phosphoric acid can then be omitted. In the case of high-tungsten steels, 5 ml of hydrofluoric acid is added to complex tungsten in the early stages of the method, this being independent of the 10-ml addition subsequently made at the titration stage. In the case of high-manganese steels, it was noted that the presence of fluoride encouraged the formation of chromate during the preliminary oxidation, but since appreciable amounts of tungsten are seldom found in high-manganese steels this limitation was considered unimportant.

Chromium Interference

Chromium gives a background colour interference when the method operates on an unfavourable weight basis for samples of high chromium and low vanadium content. The endpoint becomes very difficult to detect and it is advisable to remove some of the chromium with a preliminary separation. A mercury-cathode separation is a very obvious choice but the much simpler and more rapid method of volatilizing the bulk of chromium as chromyl chloride was also investigated. Small additions of hydrochloric acid are made to a strongly fuming perchloric acid solution of the sample. As it is not necessary to remove all the chromium, this addition need only be repeated once or twice, but the solution must be strongly fumed between each addition of hydrochloric acid. By adding 1% vanadium to a steel in this category (MGS/177), it is shown (Table IV) that there is a slight loss of vanadium by this procedure, but it is not considered significant for this special class of material. If the vanadium content is higher, a smaller weight of sample can be used and the difficulty of the background colour will not be noticed.

Table IV

DETERMINATION OF V USING FINAL METHOD

Analyst	Vanadium, % - Sample MGS/											
	48		166		177 (CrO_2Cl_2)		177 (Hg Cath.)	177 + 1% V	212		225	
	a	b	a	b	a	b	b	a	a	b	a	b
1	—	1·50	—	3·98	—	0·33	0·31	—	—	2·17	—	0·04
2	1·50	—	3·98	—	—	—	—	—	2·15	—	—	—
3	1·55	—	4·05	—	0·35	—	—	—	2·19	—	0·055	—
4	1·54	1·50	4·02	4·01	0·34	—	—	0·32	2·16	2·15	0·045	0·05
5	1·51	1·49	3·97	3·95	0·33	0·32	0·32 (a & b)	—	2·14	2·18	0·05	0·045
6	1·54	1·54	4·07	4·07	0·34	0·34	—	—	2·21	2·21	0·045	0·05
7	1·53	1·51	4·04	4·02	0·33	0·33	—	0·31	2·17	2·18	0·045	0·05
8	—	1·49	—	4·03	—	0·33	0·31	0·30 (b)	—	2·15	—	0·04
9	1·54	—	4·07	—	0·31	—	—	0·30	2·15	—	0·045	—
10	1·50	—	3·99	—	0·32	—	—	0·30	2·14	—	0·03	—

a = preoxidized indicator solution *b* = preoxidized indicator (solid form)

Potentiometric Endpoint Determination

In the course of the work covered by this report the Sub-Committee has been aware that some use is now being made of the potentiometer for endpoint detection during titration. However, in view of the fact that there is a definite prior need for a satisfactory normal titration method, it has not been thought advisable to hold back this report until a complete new programme had been carried out using the potentiometer. It is obvious from information already to hand that its use would make endpoint detection much easier in those high-alloy steels giving rise to background colours which interfered with the normal titration method.

There is no evidence that the use of a potentiometer improves the accuracy of the method, as the results obtained by one or two members of the sub-committees have been in very close agreement with the visual endpoint detection.

CONCLUSIONS

A method has been established which gives satisfactory performance on highly alloyed materials. After due consideration of previous B.S.I. methods it is recommended that the pre-oxidized form of diphenylamine indicator should be used at the reduction endpoint with N/40 ferrous sulphate. The use of the fluoride ion is suitable for complexing any tungsten present and an excess of fluoride is advised for improving endpoint detection.

APPENDIX

Recommended Method

Solutions

Sulphuric acid	(20% v/v)
Ferrous ammonium sulphate	(N/10 approx.)
Potassium permanganate	(0·5%)
Sodium nitrite	(0·5%)
Preoxidized diphenylamine indicator	Solid or solution as in B.S. 1121: Pt. 33: 1954
Sulphamic acid	(10%)
Ferrous ammonium sulphate	(N/40 approx.)
Potassium dichromate	(N/40) Dry about 2g of potassium dichromate in a dust-free atmosphere for 1 h at 150° C, weigh 1·2258 g of the dried salt, dissolve in water, and dilute to 1 litre.

Range

Up to 4% vanadium.

Reproducibility

At the 2% V level replicate results should not differ by more than ± 0·03% V.

Procedure

Transfer 1 g of sample (Note 1) to a 600-ml beaker, add 50 ml of sulphuric acid (20%), cover the beaker, and heat the solution at a low temperature. When action ceases (Note 2) oxidize the solution with 2–3 ml of nitric acid (sp. gr. 1·42) (Note 3). Boil to remove nitrous fumes cool, dilute to 200 ml.

Add 5 ml of ferrous ammonium sulphate solution (N/10 approx.) to ensure that chromium is in the reduced state. Add potassium permanganate solution (0·5%) dropwise until a pink colour is produced which persists for at least 1 min. Add 0·5 ml in excess and allow to stand for a further 1 min. Reduce the excess permanganate with steady dropwise additions of sodium nitrite solution (0·5%), 5 drops at a time, adding 10 drops excess; add 5 ml of sulphamic acid (10%) and mix well.

Add 10 ml of hydrofluoric acid (Note 4), exactly 1·0 ml of preoxidized indicator and on full development of the violet colour (at least 1 min) (Note 5), titrate with ferrous ammonium sulphate solution (N/40 approx.) to the disappearance of the violet colour, i.e. when one drop produces no visible change in colour.

Standardization

Transfer 1 g of vanadium-free iron to a 600-ml beaker and repeat procedure in paragraph 2 above. At the titration stage, after the addition of 10 ml of hydrofluoric acid, add from a burette 20 ml of potassium dichromate solution (N/40) and exactly 1·0 ml of the pre-oxidized indicator. Titrate with ferrous ammonium sulphate solution (N/40 approx.) to the disappearance of the violet colour. Record the total volume of ferrous ammonium sulphate used (A). Add a further 20 ml of the potassium dichromate solution (N/40) and repeat the titration to the disappearance of the violet colour (B). Any difference between A and B is due to indicator blank and a suitable correction must be made to all results.

1 ml of ferrous ammonium sulphate solution (N/40) = 0·001274 g vanadium.

Notes

(1) For vanadium contents below 1% use 2 g of sample.

(2) For steels containing tungsten make a suitable addition of hydrofluoric acid at this stage; 5 ml is sufficient to retain 0·2 g of tungsten in solution. If the sample contains over 5% manganese, it is preferable to use 5 ml of phosphoric acid (sp. gr. 1·75) to complex the tungsten.

(3) When more than 0·1 g of chromium is present dissolve the sample in 30 ml of hydrochloric acid (sp. gr. 1·16) and 5 ml of nitric acid (sp. gr. 1·42) in a 600-ml narrow-mouthed conical beaker. Add 20 ml of perchloric acid (sp. gr. 1·54), cover the beaker, and evaporate the solution until it is fuming strongly and the chromium is oxidized to chromic acid. Move the cover to one side and from a dropping bottle add a few drops of hydrochloric acid and refume. Repeat this procedure two or three times, fuming strongly between each addition of hydrochloric acid. It is not necessary to remove all the chromium and so two or three additions should suffice. Finally take to fumes, cool, add 50 ml of water, heat to dissolve salts and dilute to 200 ml with water, add 50 ml of sulphuric acid (20%), and continue from the second paragraph of the Procedure. Since all the chromium is not removed it will be necessary to add considerably more than 5 ml of ferrous ammonium sulphate to reduce the chromium.

(4) When no hydrofluoric acid has been added to complex tungsten 10 ml should be added at this point. If some has already been added, the total addition should now be made up to 10 ml.

(5) A colour will not develop if vanadium is absent. This can be checked by adding a measured amount of the dichromate solution and continuing the titration.

ANDREW CARNEGIE SCHOLARSHIP REPORT

The Contraction Ratio for Work-Hardening Materials

By A. Shelton, B.Sc.(Eng.), and Hugh Ford, D.Sc.(Eng.), Ph.D.

SYNOPSIS

Tensile tests have been carried out in the elastic and elastic–plastic regions of strain of a number of commonly used metals, to determine the behaviour of the lateral/longitudinal strain ratio. Each test piece was subjected to a number of load cycles and the axial strain was limited to a maximum of 4% for each cycle. Between each cycle of load the test pieces were given a light re-machining and rested to ensure elastic recovery.

The results are presented in graphical form and show the true-stress/true-strain curve and the corresponding point-by-point contraction-ratio/true-strain relationship. The latter curve is compared with a theoretical curve based on a contraction ratio of 0·5 for the plastic component of a given axial strain and Poisson's ratio for the elastic component. In one case the permanent volume changes implied by the difference between the two curves of contraction ratio are shown.

It is concluded that, provided accurate values of the elastic constants are known, the contraction ratio may be readily computed for a given strain in the elastic–plastic region. The results show that small permanent changes in volume occur but that it is impossible to obtain absolute values by this method. 1120

Introduction

WHEN A BODY is strained by the application of a tensile stress in one direction, there is a lateral contraction in all directions at right-angles to the applied stress. Within the elastic region of strain, the ratio between the lateral strain and the axial strain is Poisson's ratio. If the axial strain is extended outside the elastic limit and into the elastic-plastic and fully plastic states of strain, a somewhat more complex relationship between the strains will exist. The total strain (or alternatively a given strain increment) is then composed of two parts, i.e. a plastic part and an elastic part.

If a purely plastic body is regarded as an amorphous material, it may be likened to a very viscous fluid. In such a case, the body will deform without change in volume, and, as can easily be shown, the ratio between the transverse and axial plastic strains will remain constant at 0·5, provided that the basis of strain measurement is the natural (or logarithmic) strain. The ratio between the transverse and axial elastic strain in an elastic-plastic body, however, will remain equal to Poisson's ratio for the body. Thus, it is evident that as straining proceeds the plastic component of the total strain will assume an increasingly greater proportion and the ratio between the lateral and axial strains will change from a value equal to Poisson's ratio to a value which will equal 0·5 when the elastic component is negligibly small.

A series of tests has been carried out on a number of commonly used materials in the elastic-plastic range of strain, in which both the lateral and axial strains were measured. The experimental and theoretical curves of contraction ratio *v.* axial strain are shown, and in one case the curve of change in plastic volume derived from the difference between these two curves is also shown.

PREVIOUS WORK

Previous attempts to measure the changes in the contraction ratio have not been particularly successful. Stang, Greenspan, and Newman[1] measured the ratio on both flat-strip and round-bar specimens of steel and aluminium alloy, up to about 18% axial strain, and the maximum values varied between 0·405 and 0·47, depending upon the material used. Gerard and Wildhorn[2] carried out tests on square-section specimens of aluminium alloy, in both tension and compression, in three orthogonal directions, limiting the axial strain to 1·6%. These results showed maximum values well in excess of 0·5 and even as high as 0·68. In both cases, electrical resistance strain-gauges were used, and no special precautions to ensure axiality of loading appear to have been taken; the results are therefore considered to be unreliable.

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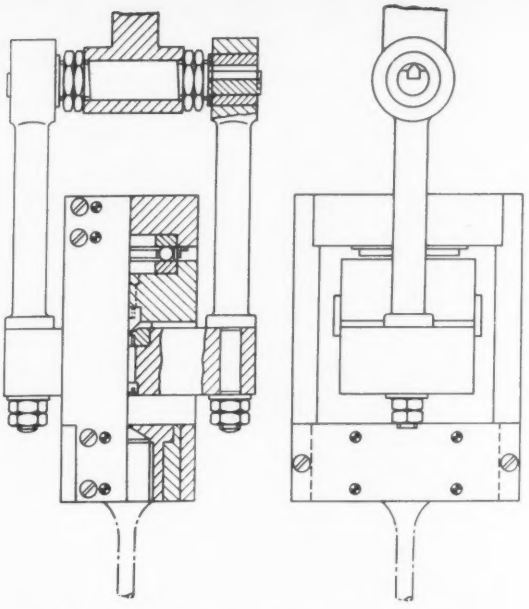

Fig. 1—Top half of axial loading shackles

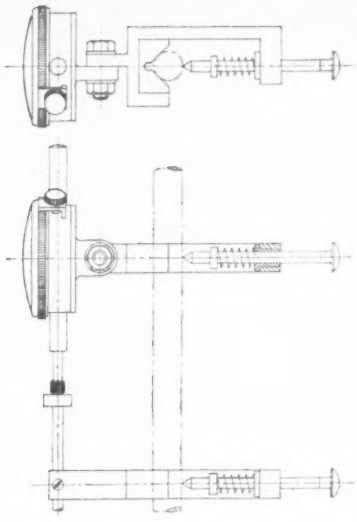

Fig. 2—Dial-gauge extensometer

APPARATUS

A 10-ton Avery lever-arm testing machine was used, fitted with axial-loading shackles (Fig. 1) of original design. Alignment of the straining load was ensured by allowing free rotation in three mutually perpendicular directions. It was considered that self-alignment would be obtained more reliably from an arrangement of knife-edge and thrust bearings than from the usual form of a ball-joint with its inherent friction.

For the main tests, a pair of Lamb's lateral extensometers and a Ewing extensometer of 5-in. gauge length were used. The lateral extensometers, located on two planes about 1½ in. apart, at 90° to each other, were adapted for a larger pair of rollers to facilitate the measurement of the relatively large strain increments experienced outside the elastic region. The two main frames of the Ewing extensometer were replaced by a longer pair, enabling the instrument to straddle the lateral extensometers when in position on the specimen, so that the lateral strains could be measured within the axial gauge length. The Ewing extensometer was calibrated against a standard Martens extensometer.

For measuring comparatively large axial strains the extensometer shown in Fig. 2 was used.* The design is suitable for round or flat test pieces, and the spring-loaded gauge points compensate for reduction in the test piece cross-section.

* The original design was by Mr. E. A. Hemsworth of Southall Technical College.

Table I

COMPOSITION AND HEAT-TREATMENT OF SPECIMENS

Material	Chemical Composition, %	Heat-Treatment
Brass	Cu 58·3, Zn 37·68, Pb 3·40, Fe 0·22, Al 0·06, Ni 0·15, Mn 0·02, Sn 0·17	Annealed at 620° C for 1 h
Duralumin	Cu 3·96, Si 0·46, Mn 0·62, Fe 0·35, Ti 0·10, Zn 0·15, Mg 0·75	As received
Copper	Sn < 0·001, Pb < 0·0005, Fe < 0·005, Ni < 0·005, Ag < 0·003, Sb < 0·002, Bi < 0·0003, As < 0·001	Annealed at 500° C for 1 h
0·26% carbon steel	C 0·26, Mn 0·60, Si 0·02, S 0·056, P 0·026	Annealed at 850° C for 1½ h
0·96% carbon steel	C 0·96, Mn 1·30, Si 0·23, S 0·001, P 0·012	Annealed at 750° C for 1½ h
En 25/A steel	C 0·33, Si 0·27, S 0·009, P 0·017, Mn 0·55, Cr 0·67, Ni 2·52, Mo 0·59	Normalized at 650° C, air-cooled Tempered at 670° C
En 25/T „	„ „ „ „ „	Hardened at 840° C in oil Tempered at 665° C
En 25/V „	„ „ „ „ „	Hardened at 840° C in oil Tempered at 625° C
En 25/Y „	„ „ „ „ „	Hardened at 840° C in oil Tempered at 550° C

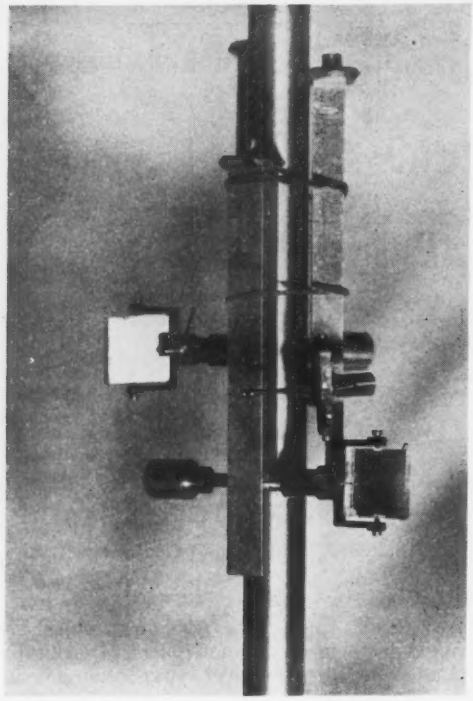

Fig. 3—Triple-mirror extensometer

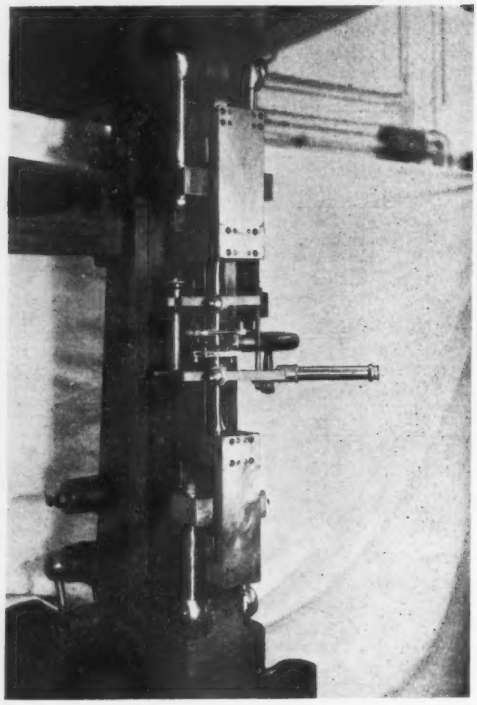

Fig. 4—Arrangement of shackles and extensometers

For the axial-loading tests a triple-mirror extensometer (Morrison[3]) was used, the three units being placed at 120° apart around the test piece (*see* Fig. 3).

MATERIALS AND HEAT-TREATMENT

Table I shows the material analysis and heat-treatment of all the specimens tested.

TEST PIECES

A typical test piece with the extensometers in position is shown in Fig. 4. The parallel length of the test piece was $6\frac{1}{2}$ in., and the $1\frac{1}{2}$-in. B.S.F. thread at each end was 2 in. long. A 2-in. fillet radius blended the parallel length with the thread, and the diameter

Table II

VALUES OF ELASTIC CONSTANTS OBTAINED FROM PRELIMINARY TESTS

Material	Test No.	Young's Modulus (E) lb/in²			Rigidity Modulus (C) lb in²			Poisson's Ratio (σ)				
		Values	Standard Deviation	Mean	Values	Standard Deviation	Mean	Values	Standard Deviation	Mean	Calculated Values*	Calculated Mean Value*
Duralumin	1	$9{\cdot}679\times10^6$	±0·22%	$9{\cdot}663\times10^6$	$3{\cdot}800\times10^6$	±0·06%	$3{\cdot}797\times10^6$	0·336	±0·97%	0·339	0·277	0·273
	2	$9{\cdot}645\times10^6$			$3{\cdot}798\times10^6$			0·340			0·271	
	3	$9{\cdot}645\times10^6$			$3{\cdot}794\times10^6$			0·334			0·279	
	4	$9{\cdot}645\times10^6$			$3{\cdot}796\times10^6$			0·337			0·269	
	5	$9{\cdot}702\times10^6$			...			0·342				
	6	$9{\cdot}659\times10^6$			...			0·343				
Brass	1	$14{\cdot}60\times10^6$	±1·01%	$14{\cdot}67\times10^6$	$5{\cdot}099\times10^6$	±1·05%	$5{\cdot}159\times10^6$	0·351	±2·15%	0·352	0·425	0·421
	2	$14{\cdot}87\times10^6$			$5{\cdot}111\times10^6$			0·362			0·424	
	3	$14{\cdot}52\times10^6$			$5{\cdot}219\times10^6$			0·344			0·458	
	4	...			$5{\cdot}207\times10^6$			...			0·392	
Mild steel	1	$29{\cdot}88\times10^6$	...	$29{\cdot}88\times10^6$	$11{\cdot}63\times10^6$	±0·37%	$11{\cdot}57\times10^6$	0·281	...	0·281	0·285	0·291
	2	...			$11{\cdot}58\times10^6$			...			0·298	
	3	...			$11{\cdot}51\times10^6$			...			0·298	
	4	...			$11{\cdot}56\times10^6$						0·285	

* The four calculated values for each material, shown in column 12, were computed from the extreme values of Young's modulus and the rigidity modulus and form a guide to the order of 'scatter' which it is possible to obtain, using the relationship between the elastic constants. The first value in each case was obtained from the highest values of E and C, the second value from the lowest of E and C, the third value from the maximum value of E combined with the lowest value of C, and the fourth value from the lowest value of E combined with the highest value of C. The figures in the final column were obtained when using the mean values of E and C.

(which was of the order of 0·5 in. in most cases) was finish-ground to a limit of ± 0·0002 in.

AXIAL LOADING

If the load applied to a specimen in simple tension is eccentric, a bending stress is imposed on the direct stress. Within the elastic region it can be shown (Morrison[3]) that the ratio of the maximum fibre strain to the mean strain for a solid round bar is $[1 + (4h/R)] : 1$, where R is the specimen radius and h the eccentricity.

Results show that an eccentricity of only 0·003 in. means an axial strain error of ± 5%. Accordingly, special precautions must be taken to achieve axial loading if the lateral extensometer readings are not to be in error. The Ewing extensometer, owing to its design, measures the mean axial strain, but the Lamb's extensometers were positioned at 90° apart; and since an eccentric load would deform the cross-section into a distorted ellipse, the mean strain reading would not be equal to that given on a truly round section of equal area.

Such an error would cause a discrepancy between the theoretical and experimental contraction-ratio curves: in the theoretical curve by the extent of the error in the elastic transverse strain only (i.e. the error in Poisson's ratio), and in the experimental curve by the extent of the error in both the elastic and plastic transverse strains.

TECHNIQUE OF TESTING

To check the axiality of loading, the triple-mirror Martens-type extensometer was used in conjunction with a vertical scale and telescope. Readings from each extensometer in turn were recorded as a test piece was loaded and unloaded to within about 80% of the elastic limit. Each extensometer was then twice moved through 120° and the procedure repeated in each case. Nine sets of readings were thus obtained and load/extension curves were plotted. Axial loading requires that these elastic curves should be parallel, and the first test showed the maximum deviation from the mean slope to be within ± 0·82%, which represents, for example, a stress of less than 0·2 ton/in² at the upper yield point of mild steel. At different times throughout the whole of this work, the axiality of loading was checked and the maximum deviation of the result of nine readings never exceeded

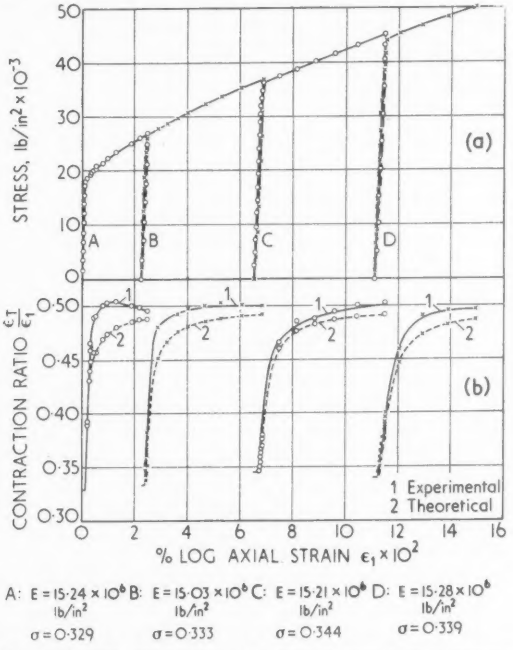
STRESS, lb/in² × 10⁻³
(a)
A
B
C
D
CONTRACTION RATIO $\frac{\epsilon_T}{\epsilon_l}$
(b)
1 Experimental
2 Theoretical
% LOG AXIAL STRAIN $\epsilon_l \times 10^2$
A: E = 15·24 × 10⁶ lb/in² σ = 0·329
B: E = 15·03 × 10⁶ lb/in² σ = 0·333
C: E = 15·21 × 10⁶ lb/in² σ = 0·344
D: E = 15·28 × 10⁶ lb/in² σ = 0·339

Fig. 5—Brass specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

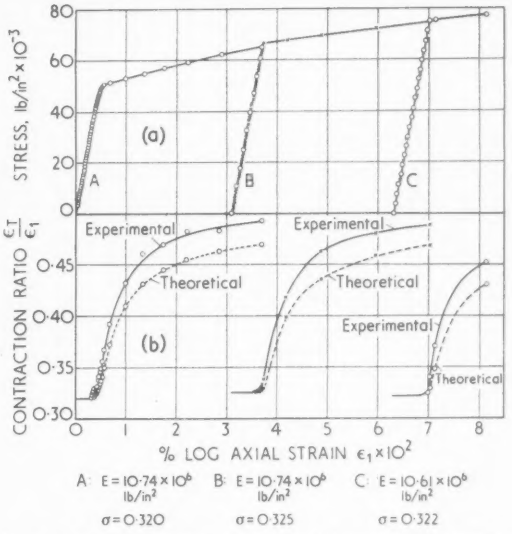

Fig. 6—1st Duralumin specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

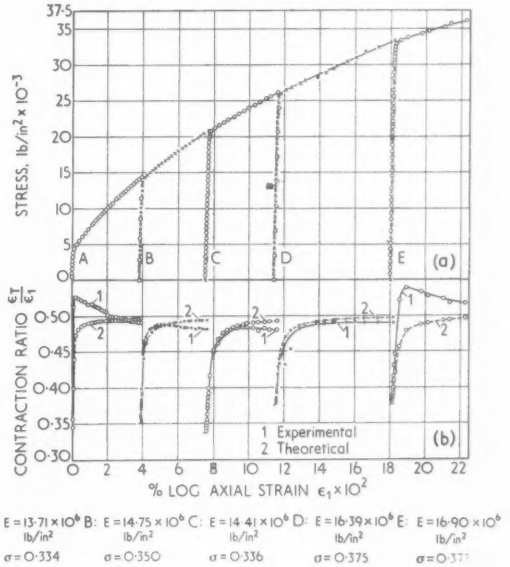
STRESS, lb/in² × 10⁻³
(a)
A
B
C
D
E
CONTRACTION RATIO $\frac{\epsilon_T}{\epsilon_l}$
1 Experimental
2 Theoretical
(b)
% LOG AXIAL STRAIN $\epsilon_l \times 10^2$
A: E = 13·71 × 10⁶ lb/in² σ = 0·334
B: E = 14·75 × 10⁶ lb/in² σ = 0·350
C: E = 14·41 × 10⁶ lb/in² σ = 0·336
D: E = 16·39 × 10⁶ lb/in² σ = 0·375
E: E = 16·90 × 10⁶ lb/in²

Fig. 7—2nd copper specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

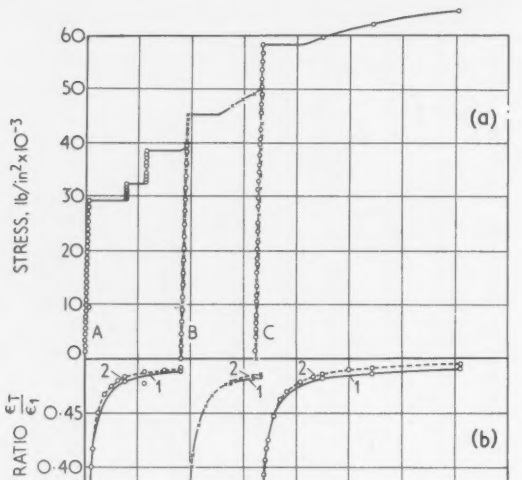

Fig. 8—0·26% carbon steel specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

$\pm 1{\cdot}32\%$ from the mean slope. It was therefore concluded that axial loading was achieved.

The degree of accuracy with which the elastic constants E and σ could be measured was now considered. Several tests using a Martens mirror extensometer were carried out on a specimen of Duralumin and a reliable value of E was obtained. A Ewing 5-in. extensometer was then used for the same purpose and the mean value obtained agreed with the previous value to within $\pm 0{\cdot}1\%$. The standard deviation of six load/extension curves from the mean slope when using this latter instrument was $0{\cdot}22\%$, and it was concluded that this extensometer would be quite satisfactory for this research, since its maximum range when utilizing the micrometer head is 0–4% axial strain.

Poisson's ratio was measured, using a single lateral extensometer in conjunction with a telescope and horizontal illuminated scale, as well as the Ewing extensometer. Loading was again confined to within about 80% of the elastic limit, and load/strain curves for the two extensometer readings were plotted. Six tests on the same specimen of Duralumin were carried out, and σ was shown to be $0{\cdot}339$. The standard deviation of Poisson's ratio from the mean of six values was $0{\cdot}97\%$.

An attempt was now made to check this value of σ, using the relationship between the elastic constants $\sigma = (E/2C) - 1$, where C is the rigidity modulus. Several torsion tests were carried out on the same Duralumin specimen, using a mirror-type torsion meter, and a value of C was obtained, which could be repeated to within $\pm 0{\cdot}1\%$. Using this value, and the value of E obtained from the Martens mirror tensile test, σ was shown to be $0{\cdot}273$. Tests were also carried out on brass and a mild steel, and the results are shown in detail in Table II. The discrepancy between the values of σ obtained by direct measurement and from the relationship between the elastic constants is quite large but nevertheless consistent with results shown by other workers. Kaye and Laby[6] quote even larger discrepancies for certain metals, and Dudzinski[7] shows results which when interpreted in terms of differences between measured and calculated values of σ agree very well with the results quoted in this paper. This is particularly true of his results for Al–Fe alloy.

This method of checking Poisson's ratio was rejected for the following reasons:

(i) Small errors in the measurement of E and C are immediately multiplied when inserted in the expression relating these values with σ (*see* col. 12, Table II).

(ii) The relationship between the elastic constants only applies to an isotropic material. In torsion, the principal planes are inclined at 45° to the axis of the bar, whilst in tension they are inclined at 0° and 90°, so that unless particular care is taken in annealing,

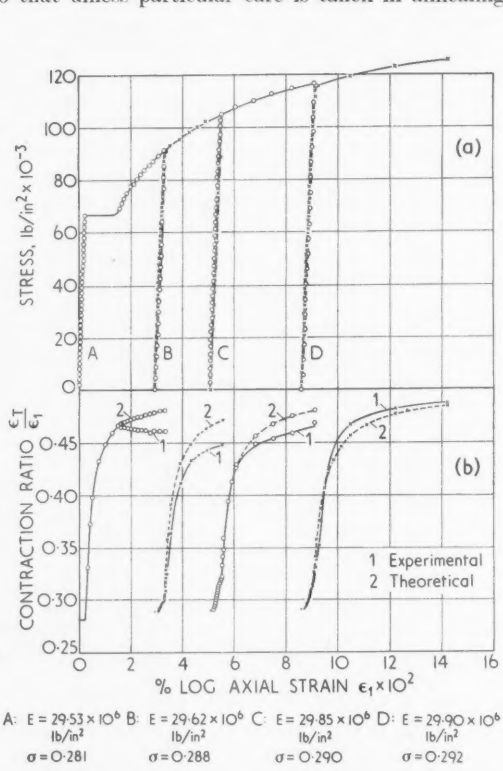

Fig. 9—1st 0·96% carbon steel specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

a certain measure of anisotropy, due to the preferred orientation of the crystal structure set up in the manufacturing process, will always be present.

From the experience gained in these preliminary tests it was decided to use two lateral extensometers at right-angles, about $1\frac{1}{2}$ in. apart. A simple fixture retained the extensometers in this position and yet allowed them to move apart with the axial strain. If any inherent anisotropy were to cause the initially circular cross-section of the specimens to develop an elliptical contour, as straining proceeded, then calculations showed that the maximum error involved in taking the mean of the two lateral extensometers' readings would be negligible. Even when the major axis of the assumed ellipse is 10% greater than the minor, the error does not exceed $+ 0{\cdot}5\%$ for any orientation of the extensometer. The basis of comparison was a circle of area equal to that of the ellipse.

With this new arrangement, Poisson's ratio was measured in six tests which showed a standard deviation of $0{\cdot}80\%$ from the mean value. This order of accuracy was high enough to justify extending the tests into the elastic-plastic region.

The Ewing extensometer was first calibrated over its full range of 0–4% strain. The calibration factor over each full graduated-scale increment showed a maximum deviation of $\pm 2\%$ from the mean value. As the full-scale deflection represents an axial strain of $0{\cdot}5\%$, the maximum error in using the mean calibration factor represents a strain of $\pm 0{\cdot}01\%$. This may be neglected since the maximum deviation

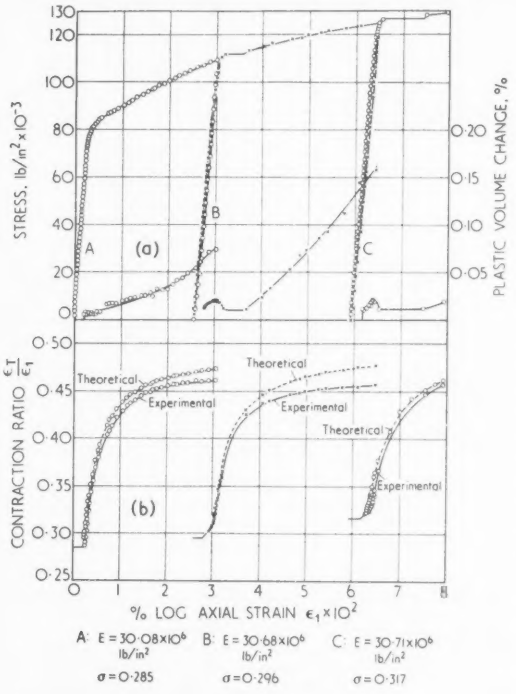

Fig. 10—1st En 25/A steel specimen: (*a*) true stress/strain curve and plastic-volume-change/strain curve; (*b*) contraction-ratio/strain curve

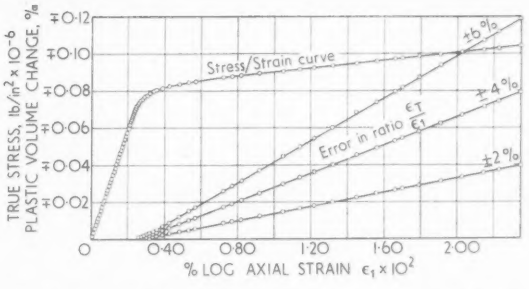

Fig. 11—Implied plastic volume change for given error in contraction ratio. Results for 1st test on 1st En 25/A steel (Fig. 10)

occurred at the end of the micrometer travel, which is only used when the axial strain is about 4%.

The testing procedure was as follows. The specimen was strained to just within the elastic limit, simultaneous readings of the Ewing and Lamb's lateral extensometers being noted. Within this region the lateral extensometers were at their maximum sensitivity, using the smallest needle rollers and steel balls. The lateral extensometers were then fitted with larger rollers and balls, and these and the Ewing extensometer were re-set to zero on the scale. Straining was then taken into the elastic-plastic region while readings were recorded. From time to time, the horizontal scale had to be moved nearer to the specimen. This procedure was repeated until the axial strain reached about 4%, whereupon the test piece was unloaded, its elastic recovery was noted, and then removed for regrinding. After allowing it to rest for at least 48 h the whole procedure was repeated until the maximum load was reached.

To ensure equalization of temperature, the entire set-up was left for several hours before starting a test, a necessary precaution owing to the extreme sensitivity of the lateral extensometer when recording elastic strains.

For those materials which exhibited a slow rate of creep (notably the steels) a time interval of 3 min was allowed between readings. When the condition was reached that the specimen was still creeping appreciably after this time interval, further load increments were reduced. Smith and Howard,[4] after exhaustive tests on a wide range of steels, found that in general the elastic recovery after plastic deformation was complete after about 48 h. All specimens were therefore rested for at least this period and complete elastic recovery was obtained, except for the En 25 group of steels, where a small hysteresis was detected after reloading; the mean slope of the load/strain curves was used for the elastic constants.

The dial-gauge extensometer was used for the large yield-point strains of the carbon steel specimens, and also replaced the Ewing extensometer when the latter had reached its full range, for the first specimen in each group of three tests of the En 25 series. It is believed that no stress/strain curves have been published for these steels, and so one specimen in each group was tested directly to fracture. Unwin's constants were also determined for these specimens.

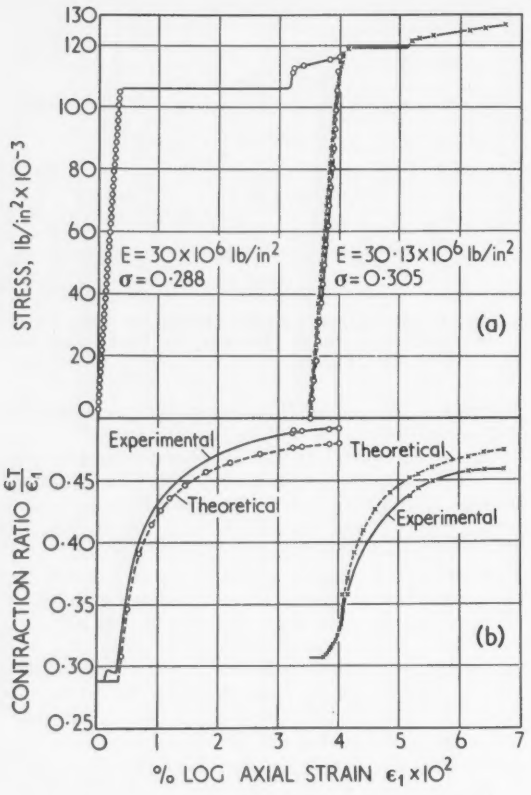

Fig. 12—2nd En 25/T steel specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

DISCUSSION OF RESULTS

The theoretical curve of contraction ratio and axial strain is obtained as follows:

Within the elastic-plastic region, the strain may be considered as composed of two parts: the elastic and the plastic.

Let ϵ_{1p} = logarithmic axial plastic strain $= \int_{l_0}^{l} \frac{dl}{l}$

and ϵ_{1e} = axial elastic strain. Thus

$$\epsilon_1 = \epsilon_{1e} + \epsilon_{1p}.$$

(*Note:* Elastic strains are so small that they are identical in both logarithmic and conventional form.)

Transverse elastic strain $= \sigma \times \epsilon_{1e}$ and transverse plastic strain $= 0{\cdot}5 \times \epsilon_{1p}$

$$\therefore r = \frac{\sigma \times \epsilon_{1e} + 0{\cdot}5 \times \epsilon_{1p}}{\epsilon_1}$$

This expression assumes that the value of Poisson's ratio obtained within the elastic region remains constant in the elastic-plastic region. Calculations have shown that if this ratio does not suddenly change when the elastic limit is passed—and there is no reason to believe that it does—then the assumption is justified.

It has also been assumed that as far as the onset of necking the strain distribution is uniform and that the transverse strain measured on a single plane by a given lateral extensometer is uniform along the length of the specimen. Careful diameter measurements after testing have shown this assumption to be justified.

Logarithmic or natural strains have been used in Figs. 5–14 because, for constancy of plastic volume, the contraction ratio is constant at 0·5 only when this form of strain is used.

The values of the elastic constants are given for each elastic line, but the scatter is such that it is not possible to establish a correlation between these constants and the degree of permanent deformation. When computing the theoretical values of the contraction ratio, therefore, the values used were those obtained from the elastic range of the particular test. In all cases, two or three complete tests were carried out on each material, but the materials were so uniform throughout each bar, and the measurements were of such accuracy that the stress/strain curves were superposable within a fraction of 1%. Only one set of results for each material is therefore given.

The results for brass (Fig. 5) are typical, but the copper curve (Fig. 7) shows exceptionally high experimental values of contraction ratio after first exceeding the elastic limit and again after about 20% axial (logarithmic) strain. This effect was found in each

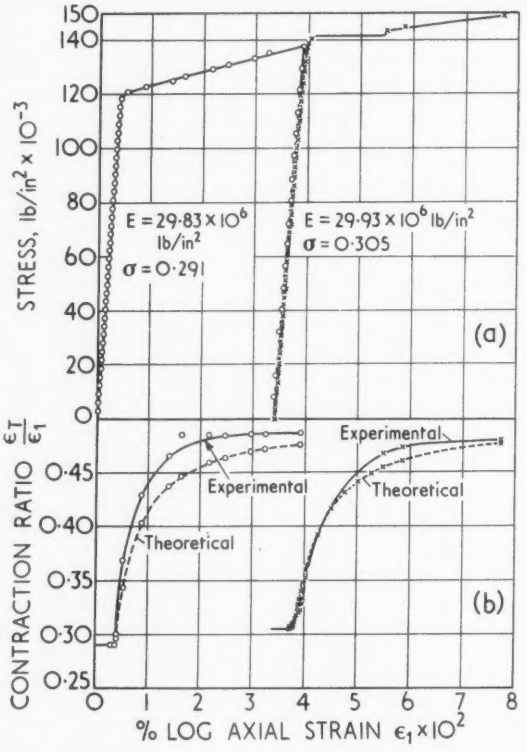

Fig. 13—2nd En 25/V steel specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

of the tests, and since it was not evidenced in the intermediate load cycles, it cannot be attributed to experimental error.

The stress/strain curve for 0·26% carbon steel (Fig. 8) amply illustrates the phenomena of strain-ageing, not only during a rest period of several days, but also during two unavoidable halts for 2–3 h. In the constant yield region the theoretical curve was obtained by dividing the yield strain for the purposes of computation. Had this not been done, the shape of the curve within that region would not be known. No experimental points could be obtained owing to the rapidity and non-uniformity of the yield process. Thus the shape of the experimental curve was influenced by the shape of the theoretical curve, but the post-yield points showed such good agreement that the procedure adopted is considered justifiable. This method of plotting the theoretical curve and estimating the shape of the experimental curve within the region of a sudden yield was adopted in all such cases in later tests.

The results for the En 25 steels (Figs. 10–14) agree closely in the true stress/strain curves, but the contraction ratio showed some differences between the first and second specimens. This is due to a number of circumstances, the main being the low rate of work-hardening and the tendency for this steel to exhibit sudden yield as in mild steel, particularly state *T*. It is interesting to note that, as pointed out by Morrison,[5] steels of this type show large yield-point strains and ageing effects. Table III shows the elastic constants and the usual test data for these steels. The elongations are much greater than the limiting values given in the British Standards.

Figure 10 shows the curve of plastic volume change implied by the difference between the experimental and theoretical values of contraction ratio for that particular test piece. It may easily be shown that the plastic dilatation

$$\Delta = e^{\epsilon_{1p}(1-2r_p)} - 1$$

where e = naperian base
ϵ_{1p} = logarithmic plastic axial strain
r_p = plastic contraction ratio.

It is apparent from this equation that when the plastic contraction ratio $r_p = 0{\cdot}5$, no plastic volume change will occur and that this is independent of the axial strain. If $r_p < 0{\cdot}5$, an increase in plastic volume is implied, and a decrease for $r_p > 0{\cdot}5$. In the case of Fig. 10 the point-by-point experimental values all lie below the theoretical curve, thus implying an increase in plastic volume, and the corresponding values have been calculated and shown.

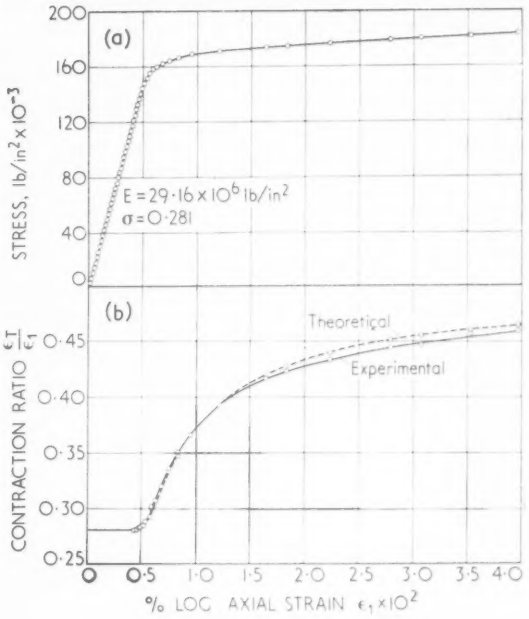

Fig. 14—1st En 25/Y steel specimen: (*a*) true stress/strain curve; (*b*) contraction-ratio/strain curve

Figure 11 demonstrates the sensitivity of this method of measuring plastic volume changes. The implied change has been plotted against axial strain for the test given in Fig. 10, using arbitrary values of error in the contraction ratio as a parameter. The volume change is in the opposite sense to the error in the contraction ratio, so that an increase in that ratio leads to a decrease in the volume. These curves suggest that small plastic volume changes do occur, since the preliminary elastic tests have shown that the contraction ratio can be measured to within at least $\pm 2\%$, whereas plastic volume changes of greater magnitude than those corresponding to the 2% line on Fig. 11 have been found.

Table III

CONSTANTS FOR THE En 25 SERIES OF STEELS

Heat-Treatment Letter	Young's Modulus, *E*, lb/in²	Poisson's Ratio ν	Yield Stress, lb/in²	U.T.S., lb/in²	Total Elongation on $4\sqrt{A}$, %	Unwin's Constants $e\% = \frac{C\sqrt{A}}{l} + B$	
						C	*B*
En 25/A	$30{\cdot}22 \times 10^6$	0·290	75,000	128,000	35·66	113·6	7·26
En 25/T	$30{\cdot}48 \times 10^6$	0·290	106,000	131,000	27·43	75·5	8·55
En 25/V	$30{\cdot}21 \times 10^6$	0·292	120,000	148,000	25·00	70·6	7·36
En 25/Y	$30{\cdot}14 \times 10^6$	0·293	145,000	185,000	18·67	55·93	4·69

CONCLUSIONS

The results show in general that there is little difference between the values of the contraction ratio obtained by experiment and those obtained from theoretical considerations. Provided that accurate values of Young's modulus and Poisson's ratio are known for a given material, the contraction ratio for a given axial strain may be computed readily. The changeover from Poisson's ratio to the fully plastic value of 0·5 is very rapid initially, and the greater part of this change occurs within the first 1% of the axial strain. Thereafter, the change approaches 0·5 asymptotically as the elastic component of the total strain becomes proportionally smaller.

The difference between the theoretical and experimental values of contraction ratio must be due to (i) experimental inaccuracies or (ii) the assumption of constant volume flow conditions. It is, however, concluded that owing to the possible sources of error, such as the measurement of the strains and the assumption of uniform straining, it is impossible to distinguish between (i) and (ii) as the cause of the difference between the curves. The results suggest that small plastic volume changes occur, but the actual values of these changes cannot be computed.

Acknowledgments

Thanks are due to the Awards Committee of the Andrew Carnegie Research Fund for the award of a grant to one of the authors (A.S.), without which this work could not have been carried out.

Thanks are also due to Mr. E. C. Larke of Imperial Chemical Industries Ltd. (Metals Division), who supplied the copper used, and to Mr. E. W. Colbeck of Hadfields Ltd., for supplying the En 25 steels.

Acknowledgments are also accorded to the Metallurgy Department of Imperial College for the analysis of some of the materials used and to the workshop staff of the Mechanical Engineering Department for their care in the preparation of the test pieces.

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Automatic Stock-Level Control on Blast-Furnaces

By I. Kjellman and K. Grönblad

SYNOPSIS

The need for better stock-level control is briefly discussed. An account is given of a successful measuring device, suggested by various authors and adopted by Oy Vuoksenniska Ab, Finland. Some details are given of a new automatic stock-level controller and of the favourable results obtained. 1178

STOCK DISTRIBUTION CONTROL on blast-furnaces mainly involves tentative adjustments of peripheral and radial distribution in order to obtain optimum performance. The conditions are very complicated and lack of accuracy in adjustment can easily produce results that are difficult to analyse. Optimum performance also requires accuracy and continuity in the influencing factors.

In the case of the conventional McKee top, radial distribution is affected by the method of charging (charging sequence), the size of the charge, the depth to stockline, the speed of the big bell, and structural alterations to the bell during shutdowns. Many blast-furnace operators affirm[1, 2] that changes in stock level are of considerable importance for furnace operation. From this it is concluded that the depth to stockline should be controlled with accuracy and preferably automatically. The measuring apparatus, as well as that for indicating and recording, should consequently be reliable.

For the measuring device there are two alternatives:

(*a*) Continuous contact by a plummet or weighted rod on stock, usually with interruption during charging

(*b*) Intermittent measuring, when the plummet is lifted immediately after each drop.

Both methods are in use, and (*a*) is preferable for closer control. However, mechanical devices are not

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very durable when foundry iron and hematite is being produced at high top temperatures, and, as far as the authors know, the application of radar or sondar[3] in continuous operation has not hitherto been successful. They have therefore chosen intermittent measuring by mechanical equipment; with this the impulses come from a cyclic timer adjustable between 2 and 15 min and from the big bell when it closes, and also, when necessary, by the hand of the supervising foreman or the scale-car operator. As a mechanical measuring device, the rod has been abandoned and instead a weighted chain[4, 5] has been adopted (Fig. 1). This runs over a pulley in a gas-tight housing communicating with the furnace. The movement of the pulley is transferred by means of a shaft, furnished with sealing glands, to a conventional wire drum and further to the stockline winch, which is equipped with movement-translating gear. There are also means for sealing off the chain housing from the furnace atmosphere by a simple valve, when the chain or the plummet has to be changed.

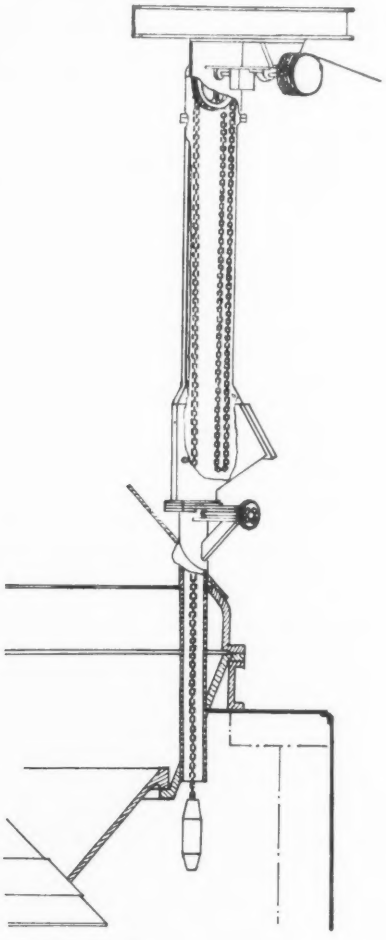

Fig. 1—Weighted chain device for intermittent measurement

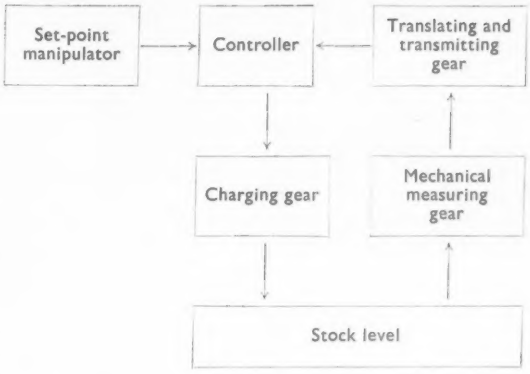

Fig. 2—Feedback control system with set-point variation

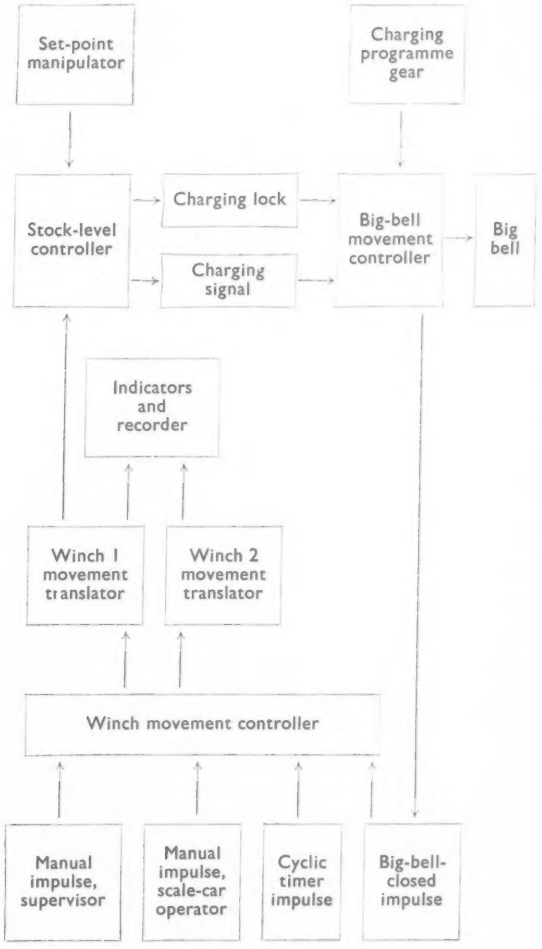

Fig. 3—Stock-level control system

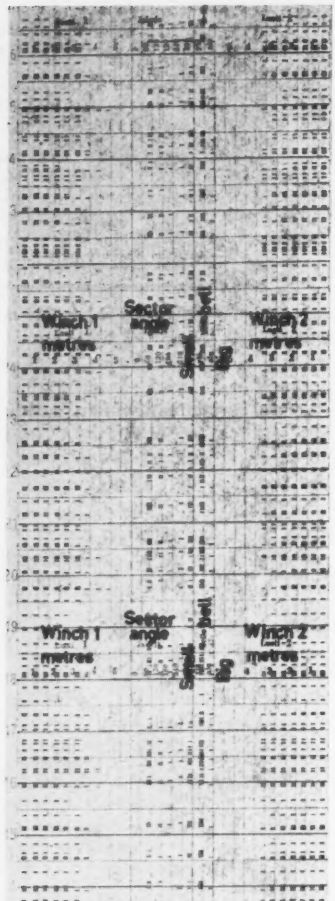

Fig. 4—Stock-level records

The movement-translating and transmitting mechanism in this case is not a continuous Selsyn-motor type or the like, but a simple cam-operated contact gear, giving impulses for each 20 in. (0·5 m) of stock-level height. This remote transmitting system was found to be accurate enough and quite satisfactory, since it is easily adaptable, with small expense, to automation and multipoint recording.

Figure 2 shows a feedback control system with set-point variation—i.e. the choice of stock level is effected manually by the supervising foreman by means of a knob. The system shown in Fig. 3 is not entirely automatic. The scale-car operator obtains from the control equipment a visual charging signal, which can also be seen by the foreman for supervising purposes. When the hopper and the big bell are discharged, the scale-car operator has to drive up the next charge immediately while the control system has blocked the big bell automatically in the closed position, until the stockline has sunk below the set point, when a new charging signal is given.

Figure 4 shows records taken at different set points of stock level. On both sides are shown the stock-level recordings and in the centre the movements of the hopper and the bells. Owing to the structural space limitation, the right-hand plummet will not go lower than 11 ft 6 in. (3·5 m).

Performance Results

(1) It is convenient for the supervising foreman to be able to alter depth to stockline by the turn of a knob.

(2) The depth to stockline is kept fairly accurate, with tolerances for the effect of charge size and of slips during blast-pressure decrease on casting.

(3) An alteration of the set point by one or two steps from the optimum performance position is enough to have a very marked and unfavourable influence on production and coke rate and to cause hangings and slips.

(4) The equipment has so far worked quite satisfactorily during 5500 h of furnace operation.

(5) Last but not least, it has been an advantage to have the human factor eliminated.

This work has been done as part of a programme making for better accuracy and a wider range of variation in controlling the influences upon stock distribution. The next step, already started, will comprise new equipment for peripheral distribution to facilitate variation of the sector-angle division and more accurate small-bell and hopper revolving movement. Automatic measurement and charging of coke will probably follow later.

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DISCUSSION ON PAPERS

Discussion at Meetings · Written Contributions

Annual General Meeting, 1955

The ANNUAL GENERAL MEETING of THE IRON AND STEEL INSTITUTE was held on Wednesday and Thursday, 27th and 28th April, 1955, at the Offices of the Institute, 4 Grosvenor Gardens, London, S.W.1. The Hon. R. G. LYTTELTON, the Retiring President, was in the Chair at the beginning of the meeting, his place thereafter being taken by Sir CHARLES BRUCE-GARDNER, BT., the new President.

THE GAS-TURBO-BLOWER FOR BLAST-FURNACE SERVICE

This discussion was based on the paper by Mr. C. E. Sayer entitled "**The Gas-Turbo-Driven Blower for Blast-Furnace Service,**" which appeared in the April, 1955, issue of the *Journal* (vol. 179, pp. 359–371).

Mr. **C. E. Sayer** (McLellan and Partners) presented the paper.

Mr. **W. F. Cartwright** (Steel Company of Wales Ltd.) : A great deal of entirely new blast-furnace plant will be designed during the next few years. I feel certain that it will not be long before a blast-furnace plant is built, fitted with a gas-turbine blower and heat exchanger, in which the exhaust heat is recovered in the blast. It is generally agreed that there is no particular difficulty in designing the gas turbine. The difficulty lies in the heat exchanger and the control circuit, if the heat exchanger is to be capable of raising the blast to the temperatures used in modern practice.

Did the author choose the temperature in the diagram because he felt it was the maximum for which the heat exchanger could be built today ? When comparing a conventional steam-turbine axial blower and stoves with gas-turbine blowers and heat exchangers, the best practice in both cases should be compared. The author has not used the best steam practice in his comparison.

A modern steel plant built on a green field site using very high steam pressures and temperatures, the steam being reheated after passage through the topping turbine and then used at more normal pressures (say 600 lb/in^2 in blower and alternators), would have a thermal efficiency higher by several percent. than that mentioned in the paper. The modern stove is extremely efficient and reliable and blast-furnace managers are not anxious to change it. The operator must have a fairly big prize if he is to be tempted away from the present satisfactory methods of operation. The main prize in the gas-turbine cycle seems to be the saving of water, which is a great difficulty in many inland steelworks.

This question of water is so important that I wish the author had talked more about it and also given more comparative information about the efficiency of the most modern type of steam plant with its possibility of very high blast heats, compared with the cheaper gas turbine and heat exchanger.

Mr. **F. Kennedy** (Dorman Long and Co. Ltd.) : I share one of Mr. Cartwright's points of criticism and consider that the author has favoured the gas turbine by not making his comparison with a steam blowing plant with good operating conditions and an efficiency which would result from high steam temperatures and pressures.

When comparing the economics of gas-turbine and steam drives for blowers it must be appreciated that the primary objective is to obtain the maximum overall thermal and economic efficiency in an integrated works. For efficient blast-furnace gas distribution it is essential to have a buffer consumer capable of absorbing the fluctuations in blast-furnace gas supply and demand. Most operators agree that the boiler plant is the most suitable buffer unit, and boilers can be designed so that a variety of alternative fuels can be burnt with little reduction in efficiency. If the gas turbine is to replace the steam turbine not only must its cycle efficiency be higher, but also the cost must be less of the alternative fuels used in the buffer consumer in conjunction with blast-furnace gas. In practice, the alternative fuel with a gas turbine would presumably be oil, so the potential gain in higher thermal efficiency from a gas turbine could be more than offset by the higher price of oil compared with coal and coke breeze fired on boilers, even assuming that a mixed-fuel-fired gas turbine is a good proposition. This appears to have been ignored by the author.

I am surprised at the author's statement that it is immaterial whether the compressor is an axial flow unit or a centrifugal type. As up to 75% of the gross power output of the blast-furnace gas-fired turbine is absorbed by its own compressors, there appears to be a very strong case for utilizing the higher efficiency attainable with the axial flow blower, and in some theoretical balances the differences in the relative efficiencies of the gas turbine and steam turbine can be largely influenced by the relative efficiencies of the compressors.

The blast-furnace ancillaries, stoves and blowers, must

be capable of responding to the requirements of the furnace. Rapid changes in either blast temperature, or volume, or pressure can be demanded ; and it is not clear how the author proposes to meet demands for increased blast temperature which may coincide with variations in blast volume or pressure. I agree that from the aspects of flexibility and ease of control there are strong arguments for having a separate air compressor and blower.

The author refers to the economy of space obtained by having a gas turbine adjacent to the furnace. The potential saving in space with this arrangement is reduced by the fact that in a commercial installation it would probably be necessary to make provision for a standby unit to each furnace. Also much of the volume of the orthodox hot-blast stove is above ground and I think the author has over-emphasized the saving in ground area in the vicinity of the blast-furnace which is claimed for the gas-turbine blowers.

On the other hand, I think the author has exaggerated the difficulties of providing two separate oil-storage and distribution systems. The steel plant is usually some distance from the blast-furnace, and in any case, under present conditions it is doubtful whether there is enough low-sulphur oil available to justify its use as a fuel for gas-turbine blowers.

I agree that this subject has stimulated much writing and discussion but very little action. The savings which will be obtained from the gas turbine can only be proved by full-scale trials by either the turbine manufacturers and/or the steel industry, and I wholeheartedly support the suggestion that such trials should be made soon.

Mr. **P. Boël** (Usines Gustave Boël, S.A., La Louvière, Belgium) : I should like to speak about the blower. The blast-furnace operator will demand a blower capable of producing a high volume of air at high pressure ; but his normal operating point will be for a high volume of air at a lower pressure, and therefore the blower will be nearly always operating at a point 15% or 20% below his maximum. For an engine like a blast-furnace blower working throughout the year, a slight decrease in efficiency would be expensive so that the blower and the whole machine must work at maximum efficiency at about 10–15% below its maximum power. The gas turbine, being based on the axial compressor, has its maximum efficiency at a very high driving rate, and when it is slowed down its efficiency drops. For the blower, the slight loss in power can be checked by a recuperation turbine; but from the turbine viewpoint the gas turbine will be operating below its maximum efficiency point.

I do not believe that a variable-speed gas-turbine is ideal for driving a blower unless it is put in a combined system like that designed for one of the steel plants in Belgium. There they have a constant speed gas turbine flanked by a blower on one side and an electric generator on the other. The blower will take about 60% of the power of the gas turbine and so the sum of the electric power generated and the blower volume generated remains constant. When the blower is decreased the electrical efficiency increases and the gas turbine works at constant speed at maximum efficiency. I believe that this is probably an excellent way of driving the blast-furnace ancillaries.

I was fascinated by the idea of the recuperation of the heat lost in the gas turbine. The temperature within the gas turbine itself is limited by the quality of the steel, and, as the author has stated in the paper, is limited to about 650° C. In Belgium, we work with much higher blast temperatures. Our average blast temperature is around 850° C, so that the recuperator would be impossible. Using a gas turbine, it is essential to recuperate as much of the heat lost as possible and the necessary recuperators are a principal part of the capital cost.

I should like to offer an idea put forward by one of the major gas-turbine manufacturers on the Continent. As shown by the curves on the author's diagram, a great amount of air must be added in excess of the combustion requirements to maintain the temperature at the limit of 650° C required by the gas turbine. As far as we can discover, the oxygen in the resultant combustion gases is as high as 17%. Instead of driving the air through recuperators—which in our case is not acceptable as the temperature of the blast is too high—I wonder if the gases could be used because of their high oxygen content, as a source of hot air for the combustion of the gas within the stoves. In that way not only would the waste heat of the combustion gases be used, but equally the amount of gas required in the stoves would be decreased and at the same time the economical high temperature in the stove could be maintained.

I have a strong belief in the gas turbine and I am sure that it will not be expensive, because steel plants need so much hot combustion air.

Mr. **G. C. R. Mathieson** (Power Jets Ltd.) : Whilst it is the aim of my Company to foster enthusiasm for the gas turbine, I think it is also our duty to protect it from too much enthusiasm. The reputation of the gas turbine has suffered too often in the past from its being conceived in an ecstasy of optimism but delivered in pain and sorrow. I therefore venture to criticize the author's figures of comparative efficiency.

The gas turbine shaft efficiency is not likely to be greater than that of the steam plant. At present we tend to restrict the maximum temperature to 650° C, and so the efficiency is not likely to be much in excess of 22·5%, unless the cost of the gas turbine is increased by high pressure ratio or costly and very efficient heat-exchange apparatus. Steam plant at 600 lb/in² and 700° F should give a shaft efficiency of 22·5% and at 850° F an efficiency nearer 26·5%. Pressures and temperatures are increasing on new designs of steam plant, and so with temperatures going up on gas-turbine plant the efficiencies will be roughly equal.

The high-temperature level of exhaust available for blast heating in the gas-turbine cycle is most important. Since the duty required here is one of both pressure and temperature increase, the effective efficiency of the gas turbine in pressure plus heat output is much greater than its pure shaft efficiency. In the combined heating and blowing cycle a fair proportion of the heating of the blast air takes place in the heat exchanger at the gas-turbine exhaust, and so the effective efficiency based on shaft output and heating is around 50% compared with 23·5% based solely on shaft output. The same strategem is not possible with the steam plant as the temperature at outlet from the boiler or the turbine is not high enough for effective heat transfer.

The incorporation of the air heater in the circuit so that the stack losses are made use of in the turbine is an additional feature which improves the efficiency of the cycle. Here, once again, the author has been unfair to the conventional system. I suggest that an efficiency of 80% for the stoves is probably rather low, an efficiency of 90% being the figure a good stove arrangement can attain. Even so, there is a saving of what would otherwise be a 10% loss.

In a Power Jets comparison of overall efficiency of blowing and heating, in which the blowing conditions are taken as 25 lb/in² gauge and 750° C, the combined blowing and heating efficiencies of steam and gas turbine are 61·9% and 68·5%, respectively, showing a smaller differential due to assuming better steam blowing con-

ditions and more pessimistic gas-turbine conditions. To be offset against this reduced differential, however, there is further saving in the gas-turbine plant owing to no water charges and reduced labour and maintenance costs. Exact comparison is difficult as the quality of the blast (pressure and temperature) has an influence on the relative efficiency values.

That concerns the savings in running costs. Now we must consider possible savings in capital costs. I must agree that it is time we gave practical force to the modern developments in heat-exchanger practice. Our investigations have shown that it is perfectly practicable to construct an air heater to heat the blast air to 750° C using existing material at a cost of about 12*s*. per ft³ per min compared with a cost of about £3 10*s*. per ft³ per min for stoves, or £1 10*s*. per kW heat output compared with about £9 per kW heat output, even in spite of having to use expensive high-temperature materials at the hot end. Thus there is great scope for capital saving by using metallic heaters in place of the brick types used at present. It is certainly attractive enough to encourage full-scale experiment.

The other important capital item is the blowing set itself. It is difficult to say with any conviction that the gas-turbine plant is cheaper than the steam plant : i.e., only for blowing. Obviously, considerable effort must go into cheapening the gas-turbo compressor set.

With this purpose I wish to oppose some of the author's suggestions. It is objected that the extension of range with the blast air taken from top pressure of the main compressor and expanded through a recuperative air turbine is at low blast volume and high blast pressure. Perhaps this is a fault in the presentation of the perform- This is a misleading way of stating the off-design-point performance. Perhaps this is a fault in the presentation of the performance map of the recuperative expansion set (see Baumann's Fig. 1*), in which the design point is shown on the extreme right-hand side, at the maximum of the flow range. The range of flow at operating pressure is the most important feature. Wide flow range is not necessary for the normal operation of a furnace, but it is important from the point of view of standardization of machinery and expansion of plant capacity. It is usual to install a steam blower with a bigger capacity than is required at first so that there is installed capacity available for higher flows, and the recuperative expansion set, if installed to operate at 50–60% of design flow, gives a high operating efficiency at that condition (higher even than at design point), and has the latent potential for increase. The only sacrifice is in capital cost which must be accepted if development potential is to be incorporated. The large blowing range also allows a few standard sets to be fitted into a very wide range of furnaces and the standardization will assist capital costs, which are high at present for lack of standardization.

It has been said that the scheme involves the use of an uneconomically low pressure ratio for the gas turbine in relation to the turbine inlet temperature. This is only valid for a gas turbine producing shaft power only ; it is not valid for one producing heat and shaft power, which an integrated scheme is doing. Two advantages of the recuperative expansion scheme have not been mentioned. One is that the lower pressure ratio means a lower pressure-ratio gas compressor (which must have a higher pressure than the main set to allow pressure losses in control valves, pipes, and burners), and a lower pressure-ratio main compressor and so reduced numbers of stages and lower capital cost. In addition, the air turbine, besides merely recuperating work as the author suggests, protects the main gas turbine aerodynamically from any necessary violent fluctuations of flow or pressure to the furnace. The main compressor can also have nearly vertical characteristics and therefore the highest possible efficiency.

There are a few major criticisms I should like to make :

(i) No pressure losses in intakes, ducting, combustion chambers, and heat exchangers seem to have been taken into account on the evidence of Fig. 6, and these have a considerable effect on efficiency

(ii) The gas-compressor pressure ratio should be 20% greater than that of the gas-turbine compressor to allow for pressure losses in control valves, pipes, and burners

(iii) The calorific value of the blast-furnace gas has been assumed to be 98 B.t.u./ft³, which is rather high as an average value. A better mean value would have been about 92 B.t.u./ft³; in our calculations we have taken a mean value of 90·5 B.t.u./ft³ or 650 C.H.U./lb which I think is a wiser move, as the calorific value can drop as low as 85 B.t.u./ft³.

Considering the conclusions, the suggestion of adopting a lower pressure ratio and a higher maximum temperature seems correct. For the reasons I have given, we feel that the best scheme is the one in which all the air is compressed to full pressure and the blast air expanded in a recuperative air turbine rather than the one proposed by the author. In the Power Jets scheme the pressure ratio is 4·0 and the maximum temperature is 730° C. The inlet temperature of 730° C would only be used at maximum capacity ; the inlet temperature to achieve 87·5% maximum flow is 650° C, and this would be a more usual operating point. It gives a potential 14% increase in flow volume at the design pressure or an increase from 25 to 30 lb/in² gauge in pressure at constant volume, and a normal speed of 95% maximum. It gives a total volume range of 3·5–1·0 at design pressure (all of this at high efficiency), the lowest efficiency being at what is normally called the design point. Therefore, we feel that it has better characteristics and, equally important, can be produced for a lower capital cost.

Mr. C. Ungoed (Steel Company of Wales Ltd.): The author is concerned about the cycle heat exchanger, when combined with a gas turbine, and the final air blast temperature is a particular worry. Throughout the paper he has referred to it as recuperative, but he might consider using a rotary regenerator for this purpose, as it seems to have greater possibilities in space saving, and if made of ceramic it would have better self-cleaning properties.

I do not think that the design of inter-gas seals would be difficult as the pressure differences are moderate. Apart from the increased final air blast temperature possible with the regenerator, there might be a slight increase in the efficiency of the gas turbine owing to the lower back-pressure imposed by this type of heat exchanger on the exhaust of the gas turbine.

The author's comparison of the steam and gas cycle efficiencies tends to favour the gas turbine. It must be appreciated that the combination of the cycle heat exchanger and the gas turbine is really an expedient to solve the problem of excessive waste-gas heat losses. For this reason, it is only fair to compare the similar expedients adopted in industry such as back-pressure and pass-out turbines to overcome the condenser loss of a straight steam cycle.

Dr. **D. M. Smith** (Metropolitan-Vickers Electrical Co., Ltd.) : I agree generally with the author's conclusions concerning cycle conditions but I feel that his compression ratio is too high. I doubt whether it is right to assume that stove efficiency is independent of the air temperatures to and from the stove.

I endorse the author's design of plant with a single line

* F. E. Baumann: Papers of the Affiliated Local Societies, Spec. Rep. No. 49, p. 40: 1954, London, The Iron and Steel Institute.

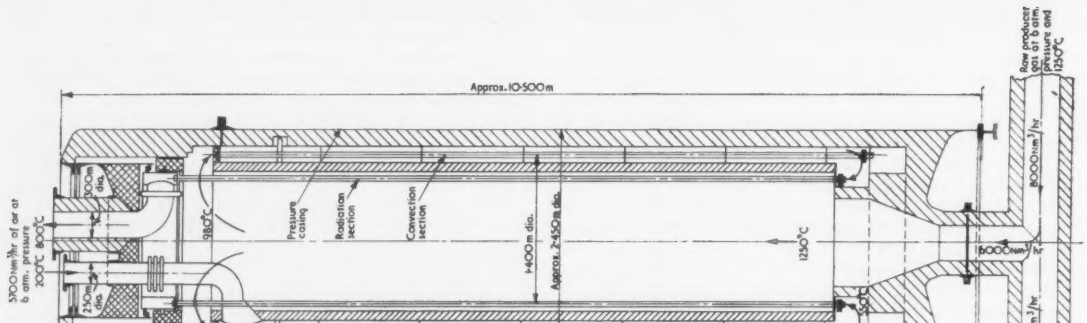


Fig. A—Heat exchanger for combined gas-turbine and high-pressure gas producer

of variable speed and with a separate axial-flow blower for reasons of performance. There is also an argument for this arrangement on the grounds of mechanical design because good design speeds for various components agree with one another. For these reasons the plant proposed by the author is superior to the tapped regenerative scheme and also to a gas-turbine plant driving a centrifugal blower.

Mr. **F. C. Evans** (Metallurgical Engineers Ltd.) : I am not an expert on gas turbines, but I have been associated with the design of heat exchangers for several schemes for blast-furnace blowing. The successful development of a gas-turbine blower for blast-furnaces depends on the development of a suitable heat exchanger. I think there is evidence in the paper that the rather complicated system which the author suggests in Fig. 6 is partly due to limitation of heat-exchanger design. On at least one of the schemes that we have studied the heat exchangers have certainly been much larger and nearly as costly as the turbine itself, which gives an idea of the size of the problem.

Whilst there is much in the paper I have not studied fully, I am disappointed that the author has not dealt with the tapped cycle combined with an air turbine, to which he refers briefly at the end of the paper. It is my experience that it is this cycle which is creating most interest at the present time. It is much simpler because it dispenses with the turbo-blower and the gas compressor, and in particular can be easily adapted to produce blast temperatures of about 750° C, which is, of course, of much greater interest than the blast temperature of about 540° C produced in the author's system.

Figure *A* shows a new design of heat exchanger which is intended for a combined gas-turbine and high-pressure gas producer, and which may have some features that might make it very useful for the cycle in Fig. 6.

The gas is very dirty, and it was therefore necessary to restrict the temperature at the entry point to the convection section to 980° C to prevent sintering, and that is why only a part of the available producer gas is used for the heat exchanger. The balanced pressures and the use of a pressure casing to contain both tube bundles enables the tube and internal casing sections to be kept to a reasonable thickness. The air flow through this unit is approximately half that in Mr. Sayer's Fig. 6, but these units could be trebled in size without difficulty.

While such a unit could not be substituted directly into the cycle shown in Fig. 6, I would be glad to have Mr. Sayer's opinion on whether such a unit could be used, with suitable modifications, and on its effect on the overall efficiency.

Mr. **T. P. Everett** (Richardson Westgarth (Hartlepool) Ltd.) : I support the author's views that the time has now come for the iron and steel industry to take the plunge and use the gas turbine for blast-furnace blowing. At least one steelworks in the U.K. is installing a unit for power generation, and whether the next unit should be for combined power generation and blast-furnace blowing or only for blast-furnace blowing is something on which I do not feel competent to advise, as I know so little about the requirements of blast-furnace managers except that they are almost always different in fact from what they anticipated at the time they ordered the blower !

I agree with the author that the single-shaft unit is the right one. Iron and steel manufacturers and engineers require the most they can obtain for the lowest capital cost, and the adoption of two-shaft arrangements—whilst undoubtedly providing considerable advantages from the part-load operation stand-point—would increase the capital cost so that the scheme would become quite unattractive.

The figures produced for the combined blowing and blast-heating schemes are very interesting, and Fig. 4 is a most ingenious presentation on which I should like to congratulate the author. I had a little difficulty in arriving exactly at some of his figures on this diagram, and the explanation may lie in the remarks the author made in introducing his paper. I would have said that the excess quantity of 650% with a turbine inlet temperature of 650° C. was a little low, and I should like to know whether the author has taken into account the temperature of the blast-furnace gas after compression, which of course would be between 400° and 540° F. That might account for the discrepancy between our figures.

AUTHOR'S REPLY

Mr. **C. E. Sayer** (McLellan and Partners): In reply to Mr. Cartwright, the blast temperature chosen for Figs. 5–9 and the results derived from them was not based on any limitation of present heat-exchanger construction. It was used as a convenient starting basis and in introducing the paper I referred to the effect on the basic

diagram in Fig. 4 of higher blast temperatures and at the end of it the effect on the comparisons is shown. It might have been better to use a higher figure—say 1200° F—but the figures given are solely for arriving at the method of presentation of the problem and could well be varied to bring them into line with the rapid advances which have taken place in the period since this work began.

Mr. Cartwright and others have suggested that I have unduly favoured the gas-turbine plant, and Mr. Mathieson has gone as far as to speak of "protecting it [the gas turbine] from too much enthusiasm". I certainly did not set out to make any exaggerated claims for it. For a modern, large-scale steam plant, using high steam pressures and temperatures, a thermal efficiency of 19·5% is obviously too low, but the figure was taken as representative of the average works.

There has not up to now been very much in favour of the gas turbine as a straight power unit because of the small difference in thermal efficiency, and the contribution to the total gains in overall thermal efficiency made by the difference assumed in prime-mover thermal efficiency to shaft output is shown on p. 367, so that anyone can deduct this in whole or in part if they wish. Also, for a large-scale plant, some would claim a higher basic efficiency for a gas turbine, even at an inlet temperature of only 1202° F, than the 23·5% assumed.

Mr. Kennedy mentioned the need for a buffer consumer of blast-furnace gas in an integrated steelworks. It is not suggested that a works using a gas-turbine driven blower would not have a boiler house to supply the large amount of steam normally required for works services. Most integrated works have enough gas surplus for electrical generation, and this—the real gas-consumption buffer—would remain to serve this purpose. Incidentally, the paper carried no suggestion of substituting gas-turbine for steam-turbine driven generator sets.

The resort to oil for keeping gas-turbine driven blowers at work does not seem to differ from the need to burn oil as a supplementary fuel under boilers, to keep steam-driven blowers and essential works services supplied with steam, when other fuels are short and electrical generation has been cut down to the limit.

Mr. Kennedy has misread the paragraph at the foot of p. 361. I did not say it is immaterial which type (centrifugal or axial) of compressor is used for the gas-turbine combustion air. I said that, with a plant embodying a separate blower as envisaged in the paper, it was immaterial whether *the blower* was of the centrifugal or the axial type. The choice between these types would only affect the speed and net shaft output to be provided for a given duty.

I had no intention of discussing the relative merits of axial and centrifugal blowers. If all the aspects of the question (e.g. the problem of water saving mentioned by Mr. Cartwright) were covered the paper would have become too long. The allusion to water saving on p. 360 is, of course, most important.

Mr. Kennedy also says it is not clear how rapid and possibly simultaneous changes in blast temperature, volume, and pressure are to be met; yet Fig. 6 shows a complete set of controls which, it is believed, would be capable of responding to these demands.

I did not refer to economy of space obtained by having a gas turbine adjacent to the furnace. The only reference to this is on p. 361, where I said that, because of the need for economizing space near the furnace, the single-line gas-turbine assembly was preferred to a two-line arrangement.

Mr. Boël spoke of a normal operating point much below the maximum operating point covered by the design. The normal operating point would generally be below the maximum design conditions, but Mr. Boël exaggerates the divergence. In speaking of the blast furnace operator demanding a blower capable of producing a high volume of air at high pressure, is he not thinking of the extra margin of pressure required to meet 'sticky furnace' conditions? Operators are usually content with a lower volume under these conditions, so that the blower load to be catered for is not much higher than under normal conditions.

With a variable-speed set in which the speed would fall at any partial load due to the characteristic of the blower, it would not be very different from the speed which a free-running compressor would reach. Therefore, the loss of efficiency with the variable-speed set should not be very great. Even at an operating point which is only one-fifth of full load, the fall of efficiency is only about $4\frac{1}{2}\%$; and therefore, for the difference between the maximum design condition and the normal operating condition, it should not be serious.

Mr. Boël says, in effect, that he does not believe that a variable-speed gas turbine is ideal for driving a blower unless it is made constant-speed. The combination of blowing and electrical generation is favoured on the Continent because Continental operators seem to be wedded to the constant-speed blower, sometimes with recuperation stages as mentioned by Mr. Boël. They are inclined to sell their blast-furnace gas to a central electrical generation body and run motor-driven blowers. That is one great drawback of combining electrical generation and blowing—it ties one down to a constant speed and sacrifices the flexibility of the variable-speed blower with its greater range of output. It is most undesirable to complicate a blast-furnace blower, which should be as simple and compact a machine as possible, by attaching an electrical generator to it.

Mr. Boël spoke of the inlet temperature for gas turbines being limited to 650° C "as stated in the paper". My choice of this temperature did not mean that I thought it the highest that could be used with present-day materials, as a number of machines are operating at higher temperatures. It was chosen as a conservative figure with due regard to reliability, long life, and the use of heavy oil as an emergency fuel.

Mr. Boël also spoke of an average blast temperature of 850° C, which is very high. I have heard such figures mentioned elsewhere recently, but only as a short-period condition. Of course, they add to the difficulties presented by the high-temperature heat exchanger, although they should hardly render it impossible.

Mr. Evans has thrown some light on developments which are taking place in these heat exchangers. An experimental plant in Canada used a special cycle involving a high-temperature heat exchanger; the experience was both interesting and promising.

Finally, Mr. Boël mentioned the oxygen content in the turbine exhaust, and what I took to be the use of the exhaust gases for the combustion of the blast-furnace gas in the stoves. I believe Mr. Everett also had this scheme in mind. From my calculations it seemed that the blower-turbine exhaust gases would represent such a high level of excess air in relation to the normal stove consumption of blast-furnace gas that it is very doubtful whether the essential top temperature in the stoves would be obtainable.

Mr. Mathieson quotes a comparison of the overall efficiencies of blowing and blast heating as giving 61·9% for steam plant and 68·5% for gas turbine plant incorporating integral blast heating. I presume that latter figure is based on the tapped compressor and recuperative air-turbine scheme, and therefore excludes the further gain, obtained with the arrangement put forward in the

paper, due to the lower final exit temperature obtainable with the exhaust heat exchanger receiving blast air at the temperature corresponding to blower delivery pressure, instead of combustion air at the temperature corresponding to compressor delivery pressure.

Mr. Mathieson's figures of comparative cost of the metallic air heater compared with stoves for temperatures of 750° C are most impressive, and certainly add greatly to the importance of further progress in their development.

Regarding Mr. Mathieson's remarks on range of flow obtained with the tapped recuperative scheme with which he is associated, the criticism in the paper certainly reflects the impression given by all the curves of bled- and tapped compressor schemes I have seen. Taking his example from the Power Jets scheme, he claims a total flow range of 3·5–1 at design pressure; but I consider that the left-hand portion of this horizontal line on the 'performance map' extends well into what I call the low-blast volume and high-pressure region, and therefore not a likely working area. I also cannot understand the remark that this 3·5–1 range at design pressure is "all at high efficiency" as the 'map' Mr. Mathieson refers to shows the overall thermal efficiency to fall away from 68·5% at design point to about 50% at the bottom end of the range.

If a line sloping downwards to the left be drawn from the design point, i.e. following the more probable working line of falling 'system resistance,' good efficiency is maintained, but in that case the volume range down to the surge line is much smaller and the maintenance of efficiency under these conditions is not peculiar to that particular scheme.

Mr. Mathieson's mention of operation at 50–60% of design flow at a higher efficiency than at design point can only relate to a falling pressure, or system-resistance, line; and the variation in efficiency from design point would depend on the slope of the pressure line. The Power Jets scheme provides a greater flow range downwards from design point along a falling pressure line than would a separate axial blower of high efficiency design; but it also involves no little loss of efficiency as compared with a separate blower scheme owing to the higher final gas exit temperature from the exhaust heat exchanger.

I did not say that the Power Jets scheme involved an uneconomically low pressure ratio for the gas turbine. My statement was intended to apply only to tapped schemes of the form where the blast air is derived directly from the delivery end of the compressor. With the interposition of the air turbine a higher pressure ratio for the gas turbine than that required for blowing can certainly be adopted.

The introduction of the air turbine has a valuable effect in reducing the imposed pressure effect at partial load because, as the blast demand falls, the pressure ratio of the air turbine also falls rapidly, and greatly reduces the pressure ratio imposed on the turbine, compared with what it would normally fall to at a given partial load. Mr. Mathieson refers to the same point in speaking of the air turbine as protecting the gas turbine from violent fluctuations originating from the furnace, but such fluctuations must to some extent be transmitted through the air turbine to the air compressor and so to the gas-turbine cycle.

Mr. Mathieson also mentions the "lower pressure ratio" of the tapped recuperative scheme; but there is nothing inherently requiring a higher pressure ratio in the non-tapped arrangement advanced in the paper, and I agree that the ratio of 5·25 on which the figures in the paper are based is perhaps higher than is desirable.

As regards Mr. Mathieson's "major criticisms," I have allowed, in the negative power consumption represented by the compression, for about 10% higher pressure ratio than is to be used in the gas-turbine i.e. about half an atmosphere, for losses in the cycle. A higher allowance should have been made on the gas compressor but this would not greatly affect the overall result.

The calorific value of 98 B.t.u./ft^3 assumed for blast-furnace gas is not high in relation to the average figure in the U.K. today. Blast-furnace operators are looking to conditions in which the value will fall as low as 90, but whether such a condition is likely to be obtained generally is another matter. The conditions on which my paper is based represent the present-day average. In any case, the exact calorific value chosen does not really affect the main issues but only the mass flows for a given output. The basic diagram (Fig. 4) is, as explained, based on the therm unit of heat in the gas.

Mr. Ungoed suggests the rotary type of regenerator, apparently for the exhaust end as well as the high-temperature end, as he speaks of its imposing a lower back-pressure on the exhaust of the turbine. It might quite possibly lend itself well to the purpose, especially for the high-temperature service. Any heat exchanger used would have to be of the 'continuous transmission,' (recuperator) type, and not a true re-generator, and the rotary unit mentioned by Mr. Ungoed would meet the case in this respect.

The comparison suggested by Mr. Ungoed with back-pressure or pass-out steam turbines is outside the scope of my paper, because it would not be generally favoured to use such machines for the supply of process steam to drive the blast-furnace blowers, which should be kept as free from outside complications as possible.

In reply to Dr. Smith's remarks, I have not presumed stove efficiency to be independent of the air temperatures to and from the stove. Figure 5 shows the effect in lowered stove efficiency of a higher inlet temperature of the blast air to the stoves. His endorsement of the design of plant advanced in the paper affords gratification, but his words seem to imply that it necessarily embodies an axial blower. This, as stated in the paper, is not so; although I agree with the use of an axial blower of present-day design for blast-furnace service.

Mr. Evans seems to find Fig. 6 rather complicated and thinks this is in some way due to limitation of heat-exchanger design. The only complication in Fig. 6 is that resulting from the indication of the convertibility of a plant on these lines from non-blast-heating to blast-heating and *vice versa*, a feature which has surprisingly drawn no comment. No consideration of heat-exchanger limitations has entered into the arrangements shown in Fig. 6, apart from those on which economic heat-exchanger design is normally based.

I cannot understand Mr. Evans's disappointment that the paper had not dealt with the tapped cycle combined with an air turbine as it contains a number of references to this scheme as the most prominent at the present time among the tapped cycles with which the 'separate blower' arrangement is compared.

The tapped cycle with air turbine does not dispense with a gas compressor. Although it dispenses with a separate blower it requires, in place of this, the air turbine, and the number of units in line is the same.

Mr. Evans also seems to think that my choice of a rather low blast temperature i.e. 540° C, as a basis, indicated a limitation of the scheme advanced in the paper. Actually the limit in both schemes would be the same, and is determined solely by the considerations of the high-temperature heat-exchanger materials and construction. It is quite possible that a unit as shown in Fig. *A*, might lend itself for this application.

I would first assure Mr. Everett that I did not overlook

the heat of compression imported into the cycle by the gas and combustion air in arriving at my excess-air quantities. Since reading my paper I have had the opportunity of checking these and I am reasonably satisfied that they are correct.

Regarding Mr. Everett's reference to the advantage of two-shaft arrangements from the standpoint of part-load operation, I would point out that with a variable-speed single-line assembly, the speed of which is allowed to vary according to the furnace demand and blower characteristic, much of the advantage of the two-line arrangement is secured with the single-line one.

DISCUSSION ON OPEN-HEARTH CHARGING

This discussion was based on the following papers, published in the March, 1955, issue of the *Journal*:

"The Supply of Scrap to Open-Hearth Furnaces," *by M. D. J. Brisby and W. O. Pendray* (pp. 252–260).

"Charging Delays Due to Furnace Bunching. A Method of Assessment," *by R. Solt* (pp. 260–264).

The papers were presented by Mr. **W. O. Pendray** (Ford Motor Company, Ltd.) and Mr. **R. Solt** (British Iron and Steel Research Association).

Mr. **R. W. Evans** (Steel Company of Wales Ltd.): Both of these papers point out a necessary and embarrassing evil which is peculiarly associated with the manufacture of steel by the open-hearth process. Mr. Solt's paper, in particular, deals with some ingenious mathematical treatment of the probability that certain delays due to bunching will occur, but the paper does not offer any remedies. Every open-hearth operator knows to his cost that bunching occurs. Much thought has been devoted to avoiding it, but unfortunately most operatives have come to the conclusion that avoiding bunching involves eventually a loss in production.

As Mr. Pendray said, if every heat made is blown with oxygen, one way of dealing with bunching is not to blow certain heats, but this only reduces output, which is not desirable. Another way of fighting it is to charge furnaces below their maximum capacity, which again means lowering output.

Another possibility is to charge an easier quality specification into the furnace, which decreases the tap-to-tap time. Instead of charging a dead-low carbon heat one might charge a higher-carbon heat and make the furnaces run a little faster, but that means that the low-carbon heat has to be made some time later. Doing this may cause trouble with the mills, who will say that the heat should be made when they want it. This is not an overall solution.

One can use the best scrap at one's disposal on certain heats, but that means that the inferior scrap has to be charged in another heat; the same thing can be said of charging extra hot metal.

There is some difficulty in organizing these processes to avoid bunching in a busy shop. It is most difficult where a shop is on a standard practice to ensure quality and smoothness of operation, because the standard practice is disorganized. For this reason the measures outlined by Mr. Solt are not desirable and are not liked by many shop operators.

Using the stage-railway system of charging, it is not always easy to arrange certain qualities of scrap into one charge on the charging track. The stage-railway system is at its best in a standardized shop routine where the same qualities and quantities of scrap go into all furnaces.

Since bunching occurs, more machines and facilities must be provided for doing the work in the shop than are necessary if the heats could be tapped in a regular manner. For instance, not only are an excess number of chargers needed, but also extra cranes must be available to take care of bunching periods, and teeming platforms will probably have to be increased up to the maximum the shop will allow. This adds to the capital equipment of the shop. Generally, when a shop is designed now, it is possible to assess the number of cranes needed in relation to the number of furnaces. But at the same time the existence of bunching periods must be taken into consideration and the number of cranes arranged to take care of these periods.

Probably the worst condition arises in connection with the soaking pits. The number of soaking pits generally associated with a given steel production is considerably larger than would be strictly necessary if the steel were coming out at regular intervals and in regular quantities. Thus there must be some wastage of fuel involved in dealing with bunched periods on the O.H. shops.

The conclusion is inevitable that this necessary evil of bunching is costing the industry a great deal; and with open-hearth furnaces tending to become larger the situation is becoming worse.

It seems very likely that, when the open-hearth furnace is superseded as a means of producing steel—as it most assuredly will be one day—this question of bunching will be one of the reasons for its decline.

It may be said that with a tilting furnace two, three, or even more charges may be tapped more or less at will, which will remove some of these troubles. But in fact they are not removed, because the only advantage of the tilting furnace is that it can make a few different qualities of steel; if some steel is left in the furnace and the furnace is charged again, the steel left in is made again, which does not bring much of a return.

The use of bifurcated spouts on large fixed furnaces divides the charge into two, but the furnace is making steel at a very high rate and two ladles have to be removed instead of one, so that the difficulties still exist.

On the other hand, with the other steelmaking processes, such as the Bessemer converter or even the electric furnace, it is possible to arrange to send batches of steel to the soaking pits in a reasonably regular and continuous manner, and the batches are not quite as large as they are with these very large fixed furnaces.

The methods advocated by Mr. Brisby and Mr. Pendray for alleviating some of these conditions are not completely acceptable, but probably open-hearth managers have already paid some attention and devoted some thought to these very problems. Where certain anomalies persist, it is fair to say that, whilst they are not incapable of solution, at any rate the solution is not quite as easy as it might seem.

The position is that large open-hearth furnaces are in widespread use, and steelmakers are faced in those conditions with the problem of getting the maximum possible amount of scrap into the boxes. If that is done, there is not a great deal more that can be done. But it emphasizes the whole question of scrap quality and grading. It might be better to pay for scrap according to its density and not its thickness. Scrap thickness is not important to the steelmaker provided that good density can be attained in the box.

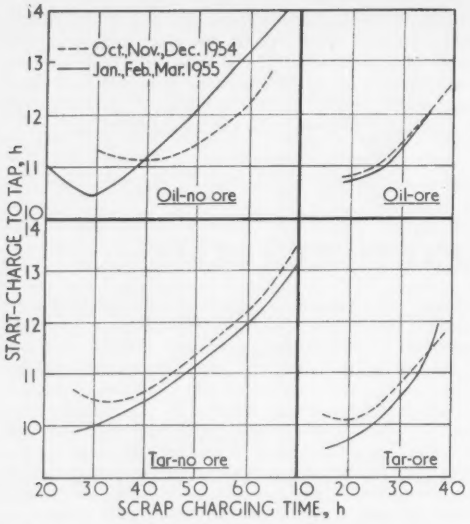


Fig. A—Scrap charging time *v.* charge-to-tap time

We have carried out some research on this point recently by assessing the density of scrap. In the first place, the dimensions of the wagon in which the scrap comes are known; it is possible to assess the volume of that wagon which is filled with scrap with fair accuracy: the weight of the wagon is known, and so it is possible to work out the density in pounds per cubic foot. For No. 1 scrap there is a tremendous variation in the density in the wagon, from about 30 to 200 lb/ft³. Nevertheless, a wagonload costs exactly the same amount of money, whether it is 200 or 30 lb/ft³.

It would be more equitable if the value of scrap were based on density. That must be the desire and object of all open-hearth managers—if possible to double or even treble the quantity of scrap which they can get into the box. Then, although this problem of bunching will not be eliminated, it will at least be alleviated.

Mr. **C. H. Bacon** (John Summers and Sons, Ltd.): The authors have indicated that they have limited their investigation to the supply of scrap to the melting shop, and they have not extended it to endeavour to discover the relationship between the rate at which scrap is charged to a furnace and output rate.

In studying the scrap supply to a melting shop, it is important to try to find the optimum charging rate, and to be able to determine the improvement in output to be expected from bringing the charging rate to this optimum.

As a representative of one of the firms at which Mr. Brisby and his colleagues carried out their studies, I should like to put forward the results of some work we have done on scrap charging rate, which is complementary to the work described in the papers. About a year ago we arranged to transfer some of the data from our furnace history sheets on to punched cards. The results have been analysed on a three-monthly basis. For this purpose the charges had to be divided into four groups according to the percentage of hot metal and the type of liquid fuel used.

At present we have not sufficient hot metal to make all our charges with 65%. Rather than reduce the percentage of hot metal on all charges, we find it preferable to make a proportion of our charges with 65% and the remainder with no ore, with a hot metal percentage of about 35%.

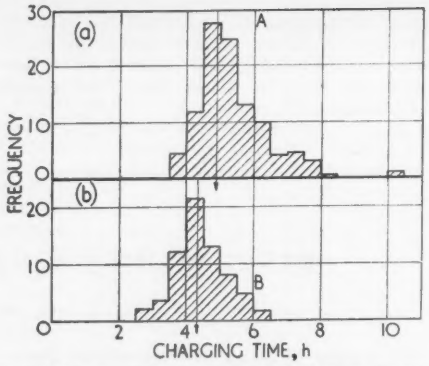


Fig. B—Histograms showing charging time frequency: (*a*) shift change during charge (average 4·9 h up to 6 h line *AB*); (*b*) no shift change during charge (average 4·33 h up to 6 h line *AB*)

Figure *A* shows the results obtained for two three-monthly periods in each of the four groups, with scrap charging time plotted against ' start charge to tap ' time. In the case of the no ore, or 35% hot metal, charges the optimum scrap charging time is about 3½ h for tar-fired and 4 h for oil-fired furnaces, in the first period. In the second period the optimum is reduced to under 3 h. We believe that this difference has resulted from a higher average pan weight during the second period.

In the 65% hot metal charges the optimum is under 2 h and may well be of the order of 1½ h.

We fully appreciate that there are many other factors that influence output rate besides scrap charging time. We believe, however, that by the use of large numbers of charges in an analysis of this type (and in this case each period includes about 1000 charges) the interference from other variables is reduced to the minimum. It is gratifying to note that the general form of the curve in each group is similar for the two periods.

We find ourselves in close agreement with the recommendations put forward by Mr. Brisby and Mr. Pendray.

Our organization for delivery of scrap to the furnaces has been strengthened recently to include one man who is responsible for the movement of traffic between the scrap shed and the furnaces. He works closely, on the one hand, with the scrap shed foreman, and on the other with the materials checker, who is responsible for the allocation of scrap and other materials to the furnaces. Special attention is paid to the reduction of bogie return time to the minimum.

An increase in pan weight has been achieved by mixing light and heavy scrap, which, together with some improvement in the general scrap quality, has enabled us to increase the average from 26 to 32 cwt. An increase in the weight of pressed blocks per bogie from about 3·6 to 4·4 tons has been obtained by the use of four peels or slippers, which can be accommodated on a bogie instead of three boxes.

Mr. **R. L. Knight** (Broken Hill Proprietary Co., Ltd.): I greatly appreciate this opportunity to say how much we in Australia appreciate the help which we have had in the past from the steel men of the United Kingdom.

I have also appreciated the papers. We all know how aggravating charging delays are and the depressing effect they have on output per hour. In these days, management rates the shop operator on how high he can go. In the U.S.A. this is especially the case.

In this discussion no one has yet asked or answered the question, " What is an ideal charging time ? " I believe it depends on at least three factors: firstly, furnace design, in relation to unit-hearth area and ton-capacity;

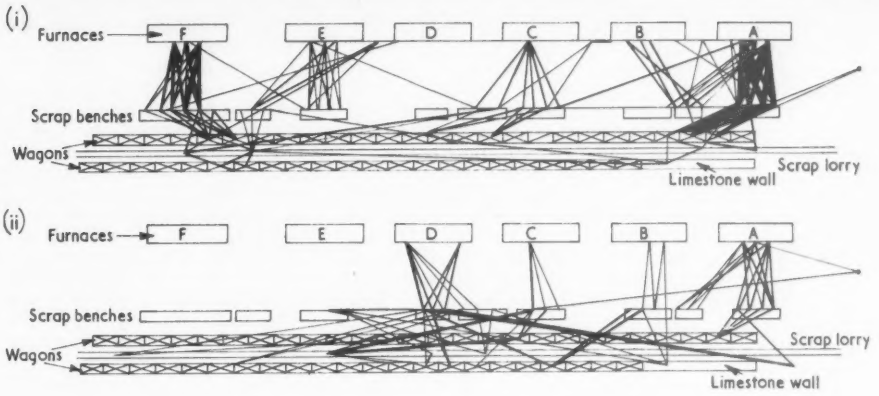


Fig. C—Patterns of crane and charger movements at Margam melting shop

secondly, the maximum fuel firing rate, as determined by the basic design of the furnace to use fuel; and thirdly, the type of practice used.

The furnaces making steel on a straight basic open-hearth practice at the fastest rates are those which have the capacity and the equipment to put cold stock in the furnace at the rate of 100 tons/h. In other words, without the equipment to put cold stock into a modern shallow hearth (a hearth not greater in depth than 33 in.) with a capacity to fire fuel up to a rate of 120 million B.t.u./h, the output rate will be inferior to the best practice.

That is the basis on which we are trying to work in Australia. We are building a modern open-hearth shop there now, and we hope to be able to cold-charge the furnaces in less than 1 h and fill them completely in less than 3 h.

Mr. **H. G. Jones** (Steel Company of Wales Ltd.): Referring to the work of Brisby and Pendray, last September we endeavoured to put a figure on the increased charging time brought about by a change of shift in a shop in which conditions were considered to be good. All the charging times (all solids and hot metal) for a fortnight were analysed into the two groups shown in Fig. *B*. Ignoring the times longer than 6 h (which indicate charging times influenced by some unduly adverse factor), it will be seen that the mean is roughly ½ h longer when a shift is interposed on the charging period. The early part of the histogram is quite different, and better when no shift change occurs during the charge.

The charging of the furnaces is influenced by the type of scrap available, and in an endeavour to build up the average type of scrap in each charge rightly advocated by Brisby and Pendray, it is quite possible that interference with the magnet cranes will occur in a shop using rotary chargers in conjunction with a scrap bench. To analyse the frequency of this, a motion study of crane movements has been carried out on a shop, shown in general outline in Fig. *C*, in which the furnaces are lettered *A–F*. The benches are shown more or less opposite the furnaces with wagons of scrap beyond the benches. The usual type of magnet crane fills the boxes on the benches with scrap. In carrying out the motion study, a string diagram was developed by fixing panel pins on the plan in strategic points, and then each time a crane or charger movement took place, the string was wound in the appropriate place; the result is a detailed picture of the crane movements, which can be treated on an hourly or a charge basis.

Figure *C* (i) shows a particular hour in the shop in which there are two furnaces charging more or less continually with a dense tight pattern of both crane and charger movements. During another hour, the pattern is quite different, as shown in Fig. *C* (ii). In particular attention is drawn to the amount of long travel done by one of the magnet cranes; this will cause delay in filling the boxes, both because of the time occupied and by the interference with the other cranes in the yard. The solution in this case happened to be another door for the entry of lorries in the middle of the shop. The general solution is, with this type of shop, that it is worthwhile marshalling scrap so that both good and bad are available with the minimum long travel of the cranes.

When it is desired to make special arrangements to overcome bunching this argument is not so valid.

What has been ignored in this discussion, as well as in Mr. Solt's paper, is that when bunching occurs it tends to keep on occurring. When oxygen is used, furnaces are much more under control; but when furnaces using oxygen bunch they will keep on bunching together. The value of an unbunching drill is more important when using oxygen than it is under other conditions.

Whilst I agree with all the drawbacks Mr. Evans enumerated, it can be shown mathematically that even with these conditions it is still worthwhile to apply the unbunching drill, because it will produce an increase in output of 2%. It may be necessary to hold back a furnace, but it will pay to do so because the other furnaces can be tapped more quickly and the whole cycle of operations goes more smoothly. I seriously believe that this is a tool of management which will have to be applied systematically in the future.

Mr. **J. H. Flux** (United Steel Companies Ltd.): The authors of the papers are to be congratulated on their attack on the problem of scrap handling and charging, and in particular the paper by Mr. Solt is a novel attempt to relate the effect of poor scrap and inadequate charging facilities to furnace operating time. Unfortunately, however, if I may deal with the two papers together, the authors have not gone far enough in their positive suggestions. There is much more to it than just close attention to melting-shop organization and management, and the problem will not be overcome merely by attending to these aspects, and in particular to shift changeover.

Robertson and Thring* have studied the effect of many variables on the output of a cold-metal furnace. Those authors were careful to point out that the furnace used for their trials had a particularly small bath area and that the quantitative results obtained could not automatically be applied to other melting shops, but they were able to draw the general conclusion that "the quantity most strongly connected to the charging and charge-plus-melt time is the mean rate at which oil can

* F. L. Robertson and M. W. Thring, *J. Iron Steel Inst.*, 1949, vol. 163, pp. 31–50.

be burned in the furnace during this period." The operative words are perhaps "burned in the furnace," and since 1949 considerable work has been done at Ymuiden and elsewhere on the efficient burning of oil. Fuel rates have been increased and in quite a number of melting shops the oil rates now being carried are such that the old statement that "an open-hearth furnace can be charged too fast" no longer applies. The emphasis is brought, therefore, on to scrap handling and charging, and the importance of speeding up these operations cannot be overstated.

Possibly the easiest way of speeding up the overall operation is by increasing pan weights, and I should like to refer to Mr. Brisby's remarks on the limitation of the press. There is still the tendency to consider that a bundling press is capable of handling only thin sheet material. There are a number of presses of this type in operation, transforming otherwise useless light material into handleable scrap, but the press must be considered as a method of obtaining good pan weights from medium heavy grades of scrap, which form the bulk of present-day supplies. With a heavy scrap press, fed by means of a conveyor, which can be used as a sorting belt to reject extra heavy material, the difficulty of over-sized scrap stopping the operation would not arise and large bundles giving a pan weight of 2–2½ tons could be obtained instead of the 10–15 cwt obtained by loading the material loose into pans. There would be a considerable reduction in the number of pans to be handled, which will help the general supply system. Despite the weight and size of the bundles obtained by this means, the density would be less than that of a bale obtained from very light scrap and should, therefore, present no difficulty so far as melting is concerned.

Mr. Solt has been aware of the difficulties in making the necessary assumptions to arrive at his mathematical equations, for he has remarked on this several times in the paper. Nevertheless, his general assumption on p. 263 "that the charge-to-charge time T is constant and the same for all furnaces" has completely eliminated the problem. If it were possible to operate open-hearth furnaces on a constant charge-to-charge time (or, as it is better known, 'tap-to-tap time') furnace bunching would not occur and all the operators would have to do would be to provide a known quantity of scrap at a known time. The problem of scrap handling and charging would in these circumstances cease to exist, and his findings must be treated with reserve. Perhaps he would give an indication of the extent to which this assumption affects the general result.

A little earlier in his paper Mr. Solt, in equation (4), has given the conditions for no delays to occur, but from his previous equation (3) this is not quite correct, because, if the furnaces become ready for charging at equal intervals t h after Rt h to prevent delay on the $(R + 1)^{\text{th}}$ furnace, the following conditions must apply: weight in boxes left from original supply plus weight in boxes returned has to be greater than weight required to charge next furnace:

$$(nW - RW_c t_c) + RW_c t > W_c t_c$$
$$\therefore (nW - RW_c t_c) > (t_c - Rt)W_c$$

This equation is in the same form as that given by the author, but he has substituted t_r for t_c. Admittedly, previously he has stated that t_c is greater than t_r, and therefore the substitution does not affect the mathematical validity of the equation, but the reason for doing so is somewhat obscure; one wonders whether this has been done to bring the theoretical formula more in line with some known practical conditions.

It is always difficult to establish the relationship between theoretical conditions and those which occur in practice, but Mr. Solt has had the opportunity of investigating scrap-handling conditions in a number of melting shops, and it would have been interesting if he could have related some practical examples with his theoretical average delay per charge given in eq. (11).

Mr. **W. L. James** (John Lysaght's Scunthorpe Works Ltd.): The authors of both papers have stressed the importance of the human element in the problems they have studied. "If the crane driver uses his intelligence and loads his pans properly . . . if he gets on his crane and loads them when he should . . . if someone sees that the transportation of empty pans from the melting shop to the scrap shed is done at the correct time . . . if someone sees that the full pans are taken back at the right time"—in my opinion these 'ifs' are of the greatest importance, because the answers depend on management getting over to the men concerned the importance of their individual jobs and how they fit into the plan of production.

It has been my experience that time and trouble taken to sell an idea like this to workpeople has generally resulted in better and more intelligent co-operation. A steelworks consists not only of furnaces, chargers, and cranes, but also, and more important than all, of men.

The problems outlined in these papers are not new, and bunching of furnaces is an old problem. There are so many factors which may bring it about. Age and condition of furnace, furnace repairs, bank, bottom and taphole repairs, shortage of tapping cranes or of chargers, bad plant layout for traffic facilities, raw materials, even bad weather outside the shop—all can be contributing factors.

Steelplants vary a great deal in the U.K., and some are much easier to manage than others, but the facts of these papers apply to all in greater or lesser degree. I congratulate the authors on their very valuable papers, and suggest that most of the problems can be solved by common sense and good management in all sections concerned.

AUTHORS' REPLIES

Mr. **Pendray**: I should like to refer to Mr. Bacon's interesting diagram linking furnace cycle time to furnace charging time. We have ourselves devoted considerable attention to trying to answer the question—by how much will start-charge-to-tap time be prolonged for any given delay in charging?—but we have not found what we believe to be a conclusive answer. I was therefore particularly interested to see the results of Mr. Bacon's work.

The average gradient of Mr. Bacon's curves is about 45°, which means that for every hour lost in charging, the start-charge-to-tap time will be prolonged by 1 h. That seems to me to be rather high. I should have thought that the increase in start-charge-to-tap time would be less than the delay that occurred in charging.

We have analysed a large number of heats at one works in order to establish this relationship between charging time and cycle time. We have tried to exclude as many variables as possible so as to get purely the causal relationship between charging time and cycle time, and we therefore selected our sample populations in such a way that they contained only heats of a limited range of tap carbon and a limited range of pig-iron/steel-scrap ratios, in order to eliminate any bias introduced by these variables. Furthermore, each sample consisted of heats from only one furnace.

For each of our sample populations we plotted the start-charge-to-tap time against the charging time on a scatter diagram. We worked out the correlation coefficients and applied the statistically best line to fit. The gradients of the plots of our various samples varied

between 1 and ½, meaning that a 1-h delay in charging is liable to produce an increase in cycle time of 1–½ h. But we accepted these results with very strong reservations: we felt that there must be a large number of factors capable of introducing a bias into the results which we could not exclude, because we had insufficient data to identify them.

Mr. Bacon said he suspected that such variables as remained would not introduce any bias into his results, because in a large sample they would cancel one another out. However, I doubt whether this is a justifiable assumption. Consider furnace keenness, to use an overworked but nevertheless meaningful phrase: we know from experience that there are times when a furnace works sluggishly and others when it works well. When it is working sluggishly, the rate at which it will accept a charge is limited by the rate at which it will melt it down, and the charging period will be long. For the same reason (viz. that the furnace is sluggish) the start-charge-to-tap time will also be long. Heats when the furnace is working sluggishly are therefore represented by points at the top right-hand corner of Fig. *A*—i.e. they will have long charging times and long charge-to-tap times. Conversely, heats obtained when the furnace is keen will lie in the bottom left-hand corner.

However, we cannot deduce from this that when charging is delayed by a given amount, the charge-to-tap time will be prolonged by the amount shown in Fig. *A*; the long charging times in that diagram are long, not necessarily because charging was delayed as a result of supply failure, but because the furnace could not melt the charge more quickly—i.e. because it was working sluggishly. We cannot, therefore, look to Fig. *A* to tell us what will happen to the cycle time if, while the furnace is working properly, we fail to charge it in the shortest time in which it can accept a charge.

For these reasons we suspect that the delay in charging which results from failure to bring the material to the stage fast enough (as distinct from a failure of the furnace to accept the material fast enough) will cause a smaller increase in the charge-to-tap time than Fig. *A* or our own diagrams indicate. But we have reached no firm conclusion about this—it is only a suspicion.

Mr. **Solt**: I am grateful to Mr. Evans for mentioning the effect of bunching in the melting shop on subsequent processes and its effect on ladle cranes and soaking pits. Mr. Evans also suggested that it might be possible to evolve some method of paying for scrap according to density rather than size. I can only speak for myself, but I have long felt that it would be of great advantage if that could be done.

Mr. Pendray has already said something about Mr. Bacon's contribution, in which we were most interested. It is extremely difficult to obtain a reliable relationship between charging time and cycle time; in one works we met two people who had tried to derive such a relationship independently of one another. One found that for every hour of charging time saved there was a saving of 2 h on the cycle time; the other found that charging time had no significant effect on the cycle time at all.

Mr. Knight raised the issue of firing rate, and Mr. Flux mentioned the same point. This is important because one may attribute slow charging to poor scrap or to the limitations of the system, when in fact the furnace itself could not deal with the charge if it were charged any faster. Mr. Bacon has just shown us that even with his new furnaces he has reached the region where faster charging may not make very much difference to the output rate of the furnace.

We were most interested in Mr. Jones's figures for charging delays at shift changeover and also in his string diagram of crane and charger movements.

I hesitate to join battle in the controversy about whether to debunch or not, but it seems to me that with increasing control of operation it should become possible to schedule furnaces more accurately. As furnaces come more and more under control, the question of debunching —when bunching occurs—must become an increasingly attractive proposition.

I thank Mr. Flux for his information about baling presses. Baling presses are getting larger and are able to deal with bigger material than they were in the past, but of course a large press is very expensive. Installing a press would pay if the output of an existing melting shop is to be raised by say 20% or 30%; otherwise it may be difficult to justify such an investment. There is a tendency, we feel, to blame charging delays on lack of equipment (such as a baling press) rather than on the possible shortcomings of the works organization.

One firm (not in the U.K.) claim that the installation of a baling press gave them over 10% rise in output. If their claim is valid, they must have cut down their average charging time by over 1 h merely by installing a press. This seems hard to believe. It seems more likely that the improvement was due to better scrap, better organization, and harder driving of the furnaces, rather than to any single one of these causes. One is tempted to ask how much improvement could have been obtained by better organization and harder driving alone.

I am glad to take up another point which Mr. Flux raised and which I may not have made clear in the first instance; it concerns the assumption of a constant charging time, on which the bunching equations put forward in my paper have been based. Obviously furnaces cannot be scheduled for weeks or months ahead.

Figure 6 of my paper shows a comparison of actual with theoretical results. In this diagram the average cycle time of 13 h has been taken as the constant T h. The amount of error which is introduced by this assumption is most obvious in Fig. 6*g*, which shows the interval between eight furnaces beginning to charge, and how the actual intervals tail off to beyond 13 h in practice. The same effect is noticeable to a lesser extent in Fig. 6*f*, which is drawn for groups of seven furnaces. Now the purpose of Fig. 6 is to show the probability of getting short intervals between successive furnaces or between groups of furnaces. In the short-interval region, on the left-hand side of each diagram, there is good agreement between theory and practice. Although at first sight the assumption of a constant cycle time may seem unjustifiable, it only introduces an error in a place where it does not matter.

Regarding Mr. Flux's query about equation (4), this merely said that no charging delay need occur if the original stock, less the stock used by R furnaces for the period of the return time, is greater than the stock required by the $(R + 1)$th furnace during the period for which it presents a demand peak.

To evolve the method described in my paper we worked out the theory first and checked it back against actual cases. In order to see whether the theory gave the right answer, we tried to check it against practical results. We have not been able to check the entire theory from start to finish because nowhere have we found sufficient records to give us the whole story in one works. All we have been able to do is to check parts of it, and we have checked almost the whole of the theory in instalments. Although I cannot offer absolute confirmation of it, we are confident that it gives good results within limits.

CORRESPONDENCE

Mr. **H. G. Jones** and Mr. **A. M. Lee** (Steel Company of Wales Ltd.) wrote: In the mathematical part of his paper,

Mr. Solt draws the right conclusions from his premises, but we believe that the premises themselves are wrong. Mr. Solt has missed the main point about furnace bunching processes, which is that they are recurrent: i.e. if the charging periods of one group of casts bunch, there is a high probability, but no certainty, that the charging periods of the next group of casts from the same furnaces will also bunch. On the other hand, if furnaces are not bunching, there is a fairly high probability that they will not bunch on the next possible occasion. It is this fact which makes consideration of ' unbunching ' techniques important. The present writers have examined it by use of Monte Carlo methods.*

It is a great error to assume, as Mr. Solt does in his calculations on p. 263, that the charge-to-charge time T is constant, as this means that if furnaces bunch once, they will continue to bunch in exactly the same way until one of them goes down for repair. This means that the furnace bunching process is *absolutely recurrent*. Suppose four furnaces A, B, C, and D charge as follows:

A charges from a_1 to a_2 hours
B ,, ,, b_1 to b_2 hours
C ,, ,, c_1 to c_2 hours
D ,, ,, d_1 to d_2 hours

Then they will still be bunching r casts later, as follows:

A charges from $a_1 + rT$ to $a_2 + rT$ hours
B ,, ,, $b_1 + rT$ to $b_2 + rT$ hours
C ,, ,, $c_1 + rT$ to $c_2 + rT$ hours
D ,, ,, $d_1 + rT$ to $d_2 + rT$ hours

This is clearly wrong.

Mr. Solt has also assumed that the charging time is constant unless there is a shortage of scrap and has neglected the influence of variable charging periods on charge-to-charge times. These facts account for the tendency for furnaces to ' unbunch ' slowly of their own accord.

Mr. Solt's treatment, instead of confirming the statements of Mr. Brisby and Mr. Pendray (p. 253) flatly contradicts them. They say that bunching increases the charging time (and therefore to a lesser extent the charge-to-charge time). Yet they claim that unbunching has usually led to loss of production. Surely, however, if by one operation a series of bunches, each resulting in longer charge-to-charge times, and consequently low production, could be prevented, unbunching would be very worthwhile. Mr. Solt's assumption of constant charge-to-charge time implies that one unbunching operation on a bunch of furnaces would keep them unbunched for ever. Here is a contradiction. Mr. Solt is implicitly supporting the deliberate unbunching of furnaces, whereas Mr. Brisby and Mr. Pendray decry it.

Furthermore, it can be shown mathematically that in the case of the furnace bunching process, the implicit assumption made by Mr. Solt in deriving his equations (9) and (10):

$$p(t) = \frac{(N-1)!}{(r-2)!(N-r)!}\, T^{(1-N)} t^{(r-2)} (T-t)^{(N-r)} \quad \ldots(9)$$

$$T_{d(r)} = \frac{(N-1)!}{(r-2)!(N-r)!}\, T^{(1-N)} \int_{t_{c(r-1)}}^{t_{c(r)}} [t_{c(r)} - t]\; t^{(r-2)} (T-t)^{(N-r)} dt \quad (10)$$

* H. G. Jones and A. M. Lee, *Operational Res. Quart.*, 1955, vol.16, p. 108.

that furnaces begin to charge at random during any interval of time t is in general wrong. Bunching is not a stochastic process of the Markov type, and therefore the history of the furnace must be taken into account. It so happens, however, that in the particular case when t is short compared with T, the charge-to-charge time, the charging behaviour of the furnaces is approximately random. Moreover, the process of bunching is ergodic. These facts explain the apparent success of Mr. Solt's formulæ for the lower values of r (corresponding to short t) and their failure for $r = 7$ and 8 (corresponding to long t), as is shown by Fig. 6.

We believe then that Mr. Solt's equations (9) and (10) are not descriptive of the furnace bunching process over a short period of time, and in particular are useless for predicting whether or not the next charging periods on a group of furnaces will bunch, when it is known that the last charging periods on the same furnaces did or not.

A better and more accurate investigation of bunching processes can be made by use of simulation techniques, of which Monte Carlo methods are the best known. It is then possible to take into consideration all the types of variation which occur in practice, and which are referred to by Mr. Brisby and Mr. Pendray.

AUTHOR'S WRITTEN REPLY

Mr. **Solt** wrote: In their written contribution, Mr. Jones and Mr. Lee enlarge on the implications of assuming a constant charge-to-charge time T. As they quite correctly point out, if the charge-to-charge time were in fact constant, the furnaces would operate on an indefinitely repeated timetable and there would be no varying bunching. It should have been amply obvious to the contributors that no bunching theory could possibly be based on such a premise, and that they misunderstood the basis of the argument.

The relevant portion of text in the paper under discussion read: " It will be assumed that the charge-to-charge time T is constant and the same for all furnaces, so that all furnaces will begin to charge once during any one period T." The necessary restraint on which the theory is based is contained in the second half of this sentence, the concept of a constant T having been introduced as a seemingly simple way of describing it.

Taking such periods of time T at sufficiently long intervals, it can be safely assumed that furnaces begin to charge at random. The validity of the method does indeed depend on viewing the incidence of bunching over a long period of time. The aim was simply to estimate the probabilities of two, three, etc., furnaces requiring to be charged within a short time of each other; this was achieved with reasonable accuracy despite the artificial assumptions, which avoided unnecessary elaboration of the mathematics.

As Mr. Jones and Mr. Lee quite correctly point out, the equations put forward " are not descriptive of the furnace bunching process over a short period of time, and in particular are useless for predicting whether or not the next charging periods on a group of furnaces will bunch . . ." The method has never been, nor was it ever intended to be, used for this purpose, and the contributors must be thanked for pointing it out to anyone else who might be in danger of such an extraordinary misinterpretation.

Finally, there is nothing in my paper which either supports or condemns the practice of unbunching. My views on this question have been recorded in the discussion, and they are basically in agreement with those of Mr. Jones as I understand them.

DISCUSSION ON CREEP

This discussion was based on the following papers (the dates of publication in the *Journal* are given in parentheses):

"Causes of Variable Creep Strength in Carbon Steel," *by W. E. Bardgett and M. G. Gemmill* (1955, vol. 179, March, pp. 211–219).

"Creep and Rupture Data on Mo, Cr–Mo, and Mo–V Steels," *by J. Glen* (1955, vol. 179, April, pp. 320–336).

"Statistical Study of Creep and Fatigue Properties of a Precision-Cast High-Temperature Alloy," *by G. T. Harris and H. C. Child* (1954, vol. 178, November, pp. 284–290).

"Relationships between Long- and Short-Time Creep and Tensile Properties of a Commercial Alloy," *by A. Graham and K. F. A. Walles* (1955, vol. 179, February, pp. 105–120).

The papers were presented by Mr. **W. E. Bardgett** (United Steel Companies Ltd.), Mr. **J. Glen** (Colvilles Ltd.), Mr. **G. T. Harris** (William Jessop and Sons Ltd.), and Mr. **A. Graham** (National Gas Turbine Establishment), respectively.

Mr. **G. T. Harris** also reported briefly on work done, since his paper was published, on vacuum-melted specimens in connection with the reduction of scatter.

Mr. **J. O. Ward** (Hadfields Ltd.): It was originally intended that Dr. Rait should open the discussion: he has asked me to offer his apologies for being unable to attend. I have been associated with him in the investigations into the creep properties of alloy steels in the research laboratories at Hadfields Ltd. This is our joint contribution to the discussion.

Dealing with the first paper, we must congratulate the authors on an excellent investigation into the causes of variable creep strength in carbon steels. From a study of these investigations the main conclusion is that variations in the aluminium nitride contents of carbon steels are responsible for variable creep strength by virtue of their profound influence on grain size.

Unfortunately, we are at a slight disadvantage in that creep tests of alloys of a similar nature, where the aluminium nitride contents are known, have not been carried out in our laboratories. However, in recent years, a major investigation has been proceeding into the factors influencing the grain-size control and ingot panel cracking of a variety of carburizing and medium-carbon steels. This investigation has not yet been completed, but one of our colleagues, Mr. B. L. Biggs, who has been chiefly responsible for this investigation, made a contribution on this subject to the 43rd Steelmaking Conference at Ashorne Hill in Sept., 1954, in which our findings up to that date were described.

For such steels produced in both the basic open-hearth and the basic electric furnaces, over the whole range of steels we had then considered, a minimum content of about 0·015% soluble aluminium was necessary to produce a satisfactory—7–8 A.S.T.M.—grain size. This appears to confirm the authors' findings.

More recent investigations have shown that there is probably a better correlation between grain-size control and aluminium nitride content than with soluble aluminium, which again agrees with the authors' findings. These results are illustrated in Fig. *A*.

In view of the influence of the nitrogen content, in addition to aluminium content, on the solubility of aluminium nitride, it would be of interest to know the nitrogen contents of the steels quoted in Table II. We notice that the authors have carried out duplicate tests on six alloys (21, 22, 28, 30, 32, and 47). Alloys 21, 22, and 47 show similar contents of soluble aluminium with minor variations in creep strength. In the case of alloys 30 and 32, we notice that the results represent different billets. Do these also represent different ingots? Are they heat-treated at the same time? If that is not so, the inference regarding production control is disturbing. Alloy 28 seems anomalous, the higher soluble content

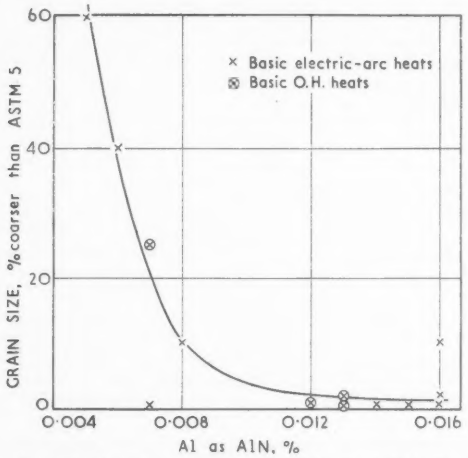
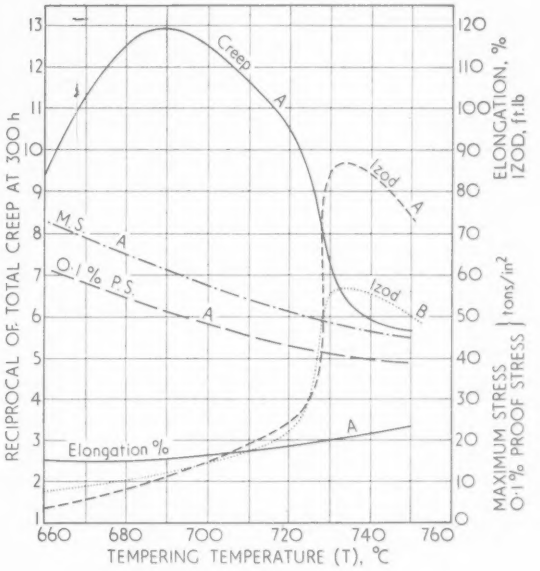


Fig. A —Grain size control of 0·4% C, 1·2% Cr steel



Creep tests: 8 tons/in² at 600° C
Heat-treatments: A. 1150° C (½ h), air; T° C (4 h), air
B. As A. + 600° C (1000 h), air

Fig. B—Effect of tempering on mechanical and high-temperature properties of 3% Cr–Mo–W–V steel

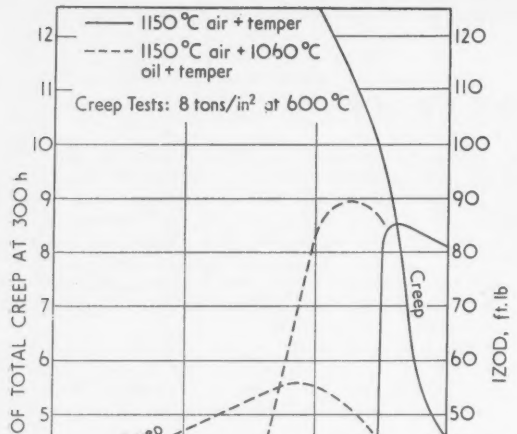


Fig. C—Creep and impact tests on 3% Cr–Mo–W–V steel

being associated with the higher creep strength. It may be that this is simply a matter of experimental error, but we should welcome the authors' comments on it.

We are pleased to have confirmation of the influence of aluminium nitride on grain-size control and feel that the authors have made a valuable contribution to our knowledge of the creep properties of carbon steels.

I turn next to the second paper. Mr. Glen has again made a unique contribution to our knowledge of the creep behaviour of the low-alloy steel, and it is of great satisfaction to us that, in explaining the long-term observations, he arrives at similar metallurgical concepts to those we postulated to explain the data accumulated from short-term investigations on more complex alloys. The important fact is that the creep properties of a given alloy at a certain temperature depend on the deviation of its constitution from the equilibrium state appropriate to the temperature and stress conditions under examination.

The rate of approach to the equilibrium state in service will affect the resistance to creep deformation, since this approach to the equilibrium modifies the distribution, size, and constitution of the precipitates: hence their influence on the movement of dislocations and thereby all the mechanical properties and the creep strength.

We should welcome more information from the author about the other mechanical properties of these alloys as affected by tempering on the lines he has shown for creep in Fig. 2. Does he, for instance, find the effect shown in Fig. *B* where the most profound changes in creep resistance occur with no significant change in the tensile properties? In Fig. *C* the full curves are the creep and impact curves from Fig. *B*. The effect of altering the condition before tempering is shown to be profound.

It is of interest to note that the tendency for this 3% Cr–Mo–W–V steel, when it is normalized and tempered at 730° C, to a guaranteed impact level of 40 ft.lb, is to exhibit poorer short-term creep properties (i.e. up to 1000 h) than the same material tempered at 685° C; but when periods of over 10,000 h are considered, the material tempered at 730° C is far superior to that tempered at 685° C. This adds emphasis to the warning given by the author that short-term investigations may mislead when extrapolated to provide long-term data. Consequently, the investigation of new composition fields for long-term applications must remain a difficult task.

The third paper is a most timely and valuable contribution on the properties of investment-cast moving-blade materials. The process of investment casting has been widely used in the U.S.A. in the production of both moving and stator blades for jet engines, but in the U.K. it has been essentially confined to the production of stator blades, because the designers have been reluctant to use cast moving components. From the authors' opening remarks it could be inferred, no doubt incorrectly, that investment-cast moving blades are used in the U.S.A. because they can produce sounder castings than we can in the U.K. This is in fact a fallacy.

In approaching the problem of jet-engine design, the U.S. designers have borne in mind the difficulty of producing such complicated components as blades, and have first of all decided that if the casting method is to be used, the ultimate design of blade should be suited to this method of production. In the U.K. the emphasis has been on efficiency of design, even if this means expensive and complicated methods of production.

It would appear that, in the future, moving blades will have to withstand higher temperatures and stresses and so such alloys may be virtually unmachinable and unforgeable. It appears that the most suitable alternative method to machining from forged or extruded material at the present time is investment casting.

The present contribution is therefore extremely valuable and indicates that serious investigation is desirable in this important field. At Hadfields we have carried out extensive investigations into the properties of cast blading materials, and generally we confirm the authors' conclusions; but we suggest, as a result of our experience, that castings can be produced with less scatter in properties than the authors show.

For example, we have found that in a wide range of alloys great improvements can be effected by scrupulous attention to the foundry technique, such as the design of ingates and of feeder heads, casting temperature, and in deoxidation practice, pouring speed, etc. In particular, it is essential to continue these experiments beyond the point at which castings appear to be sound radiographically, and we have been able to make improvements in mechanical properties by such continued attention.

Our attention was focused on this problem of micro-unsoundness in apparently radiographically sound castings by pneumatic fatigue tests on a 12% Cr steel which showed an intolerable scatter in results. By careful attention to the casting technique we have been able to produce consistently castings with properties comparable with wrought material. As illustrated in Fig. *D*, the fatigue properties of the precision-cast specimens compare favourably with those of wrought material.

These tests were carried out in resonance at a speed of 700 c/s at room temperature, the mode of stressing being simple cantilever bending. Figure *D* shows results for 12% Cr steel, 0·2% carbon steel, wrought material, and cast material. A large number of specimens were tested, not all of which are shown in the diagram. The scatter was no greater for cast than for wrought material.

In addition to meeting the standard casting specifica-

tions, for example B.S. 1630 and D.T.D. 666 and 705, we have found that it is possible to meet a number of wrought specifications (e.g. En.8 and En.29). We have also manufactured moving blades in our Era.MM.35 alloy which have been in service for a very long time and are still behaving satisfactorily.

We should confirm the authors' experience of wide scatter from the miniature Wöhler fatigue machine, which in our experience has occurred at speeds of about 10,000 rev/min, and has been invariably associated with visible resonant vibration effects.

It would be of value to know the composition variations from flask to flask in the castings tested by the authors, since this may contribute to the scatter obtained and is a variable not necessarily associated with wrought material. It would also help to see similar histograms to Figs. 3 and 5, obtained for equivalent wrought material in the same machines.

In conclusion, we hope that the authors' excellent contribution will stimulate intensive investigations into the properties of investment castings, including such important factors as grain-size control, vacuum casting, high-temperature fatigue and creep testing, etc., so that this potentially valuable method of producing moving blades will become acceptable to designers in the U.K.

Mr. Graham and Mr. Walles have presented a paper which has far-reaching implications if the results are applicable to other alloys. Unfortunately, we have been unable, owing to the limitation of time, to examine our materials in the extensive manner required to test the validity of their formula. When, with great reluctance, we are forced to extrapolate our short-term work, we use the Larson and Miller parameter, which gives an accuracy of $\pm$ 10%.

Do the authors consider that they can achieve a higher accuracy than, say, $\pm$ 5% in predicting 100,000-h data from tests of 1000 h maximum duration? If so, their achievement is of great commercial interest, in addition to its obvious academic merit.

Whilst the tendency of our research is to concentrate on the metallurgical constitution of the alloys and its relation to creep strength, we have from time to time examined our data in the light of various theories. It is a matter of interest that we usually find that the Andrade formula gives the most consistent agreement with observed creep curves, and we quote the following data regarding the values of β and κ derived from actual tests. For a 3% Cr–Mo–W–V steel tested at 600° C and 8 tons/in², we found these results:

Heat-Treatment	$\beta \times 10^{-4}$	$\kappa \times 10^{-7}$
Normalize 1150° C + 1060° C, O.Q. + temper	1·33	8·6
,, 1150° C + temper only	1·12	3·2

In the 3% Cr–Mo–W–V and in a complex 12% Cr steel we find variations in β and κ according to the stress and temperature of the test. For example, in the complex 12% Cr steel we find:

Temp., ° C	Stress, tons/in²	$\beta \times 10^{-4}$	$\kappa \times 10^{-7}$
600	15	4·27	19·0
600	14	3·54	Nil (unmeasurable)
650	8	3·13	5·4
650	7	2·3	Nil

It is our experience that the Andrade formula rarely fits creep curves up to the onset of tertiary creep (i.e. the onset of a measurable accelerating creep rate), but the formula may hold for only a short period of what appears to be second-stage creep. Perhaps the authors would like to comment on these data in the light of their own experience.

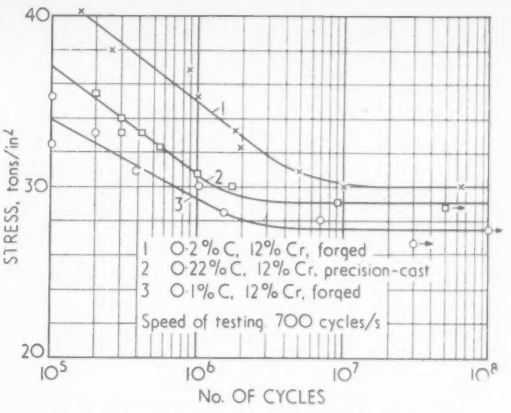


Fig. D—Fatigue properties of 12% Cr steel

Mr. **B. B. Overy** (London University): I am interested in the way in which Mr. Graham and Mr. Walles have tried to improve on the existing Nutting law, which now seems to have some well-accepted advantages and disadvantages. The chief advantage of the Nutting law as it stands is that it will fit the results of any test series which give straight-line log log graphs of stress *v.* time, strain *v.* time, or stress *v.* strain.

Furthermore, the law bridges the gap between Hooke's law and the law of viscous flow. It should be noted, however, that in tests where the Nutting law has been verified, it has been usual to adopt some definite method of loading, such as constant stress, controlled strain rate, or controlled rate of application of stress.

The results of Graham and Walles show that, if a given material is subjected to all these types of loading, parallel lines are obtained on the log stress *v.* log time graph. The inference is that, although the indices in Nutting's law are unaffected by the type of loading, the constant ψ takes a different value for the various loading histories. Thus, as an 'equation of state' the law is unacceptable if any general loading history is to be permitted. This is the disadvantage of the law in its simplest form.

In choosing a modification of the Nutting law which is more flexible in its application, it is common to choose a law governing the instantaneous strain rate. Omitting for the present the inclusion of the memory principle, it is worth examining the effect of using various simple power laws for the rate of strain. For instance, we may select the equation:

$$\dot{\epsilon} = K_1 \sigma^{\alpha_1} \epsilon^{\beta_1} \quad \ldots\ldots\ldots\

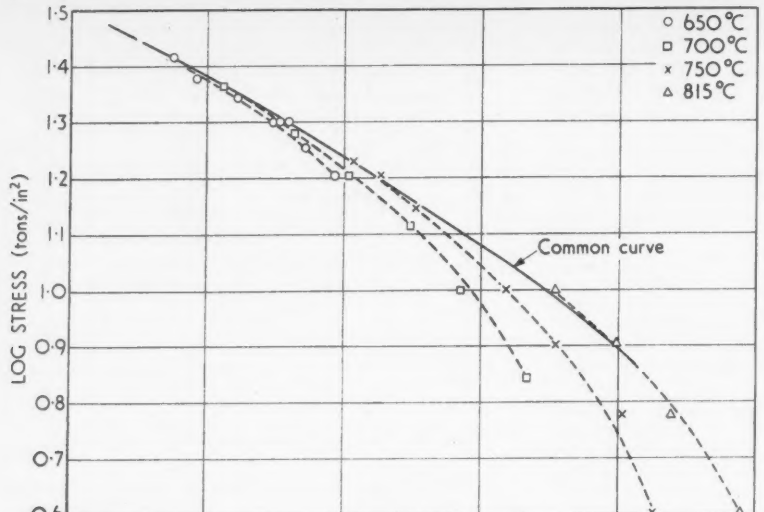


Fig. E—Log stress *v.* log(ϕ + 60) tests on Nimonic 80A

equation (i), is in a sense arbitrary. We might just as well choose:

$$\dot{\epsilon} = K_2\, \sigma^{\alpha_2}\, t^{\gamma_1} \quad \text{.....(ii)}$$

In this equation strain is replaced by time on the right-hand side and except for the addition of the memory principle we have the equation the authors have followed in the paper. This also gives the Nutting law when integrated for simple loading histories. The lines on the log log graph are again parallel, but the spacing between the lines is different from that found by using equation (i).

A number of other equations for strain rate may be devised which also give the same kind of results One may take a compromise between (i) and (ii), e.g. a law containing stress, strain, and time.

$$\dot{\epsilon} = K_3\, \sigma^{\alpha_3}\, \epsilon^{\beta_3}\, t^{\gamma_3} \quad \text{.....(iii)}$$

For simple loading histories one obtains again parallel lines on log log graphs of stress *v.* time, the spacing of the lines being controlled by the weight we put on the indices α, β, and γ. Of the above equations, (iii) is the most flexible because of the additional term.

If further evidence shows that we may expect parallel lines on plotting results for these three basic kinds of test, then a power law for strain rate is acceptable, and we wish to find that most satisfactory law. When examining the problem there seems reason to favour a law in which strain rate is a function of both the state of strain reached and the time from the start of the test. The inclusion of the energy of deformation may be of value.

While we are seeking a law with the least possible number of constants to be determined from experiments, the results of tests at constant stress, constant rate of strain, and constant rate of stress application may yield enough information to determine up to five constants in a power equation of this type.

I have made no reference to temperature-history effects or to the very considerable benefits obtained by using the memory principle, but it appears worth showing that immediate advantages are obtained when a law for strain rate is introduced. It is also emphasized that there is wide scope for the actual form of the law to be adopted. The authors appear to have decided upon a law for strain rate in terms of stress and time. Perhaps they would say whether other possibilities have been rejected or whether their choice is arbitrary.

Mr. E. D. Ward (Mond Nickel Co., Ltd.): At the Mond Nickel Company's Laboratory we have followed the work by Mr. Graham and Mr. Walles with great interest, since they have done so much upon our Nimonic alloys. It forms a great contribution to the derivation of practical methods of prediction of long-time creep behaviour from shorter-time data.

In view of that interest, one of my colleagues, Mr. T. Slimak, has applied the methods of the paper to some of our long-time creep test results on Nimonic 80*A* and Nimonic 90, using both the results which were supplied to Mr. Graham and some from more recent experiments. We have not attempted to examine to any great extent the fundamental aspects of the theory, although we have checked the modified Andrade equation,

$$\epsilon = \epsilon_0 + at^{\frac{1}{3}} + bt + ct^3,$$

and we have found that for several creep curves for Nimonic 90 the discrepancy between calculated and experimental values of strain over times up to 1800 h did not exceed 7% of the experimental values.

Most of our work has been concerned with the practical application of Mr. Graham's parameter ϕ in the prediction of very long-time creep behaviour. We find that this prediction by the authors' method must be limited in scope, particularly in the duration to which it can safely be applied. As the authors have pointed out, they do not necessarily claim to have offered the final form of such a parameter, and we put forward our findings in the hope that they may be useful in future developments.

The authors grouped together results from several sources to obtain the mass of data necessary to establish

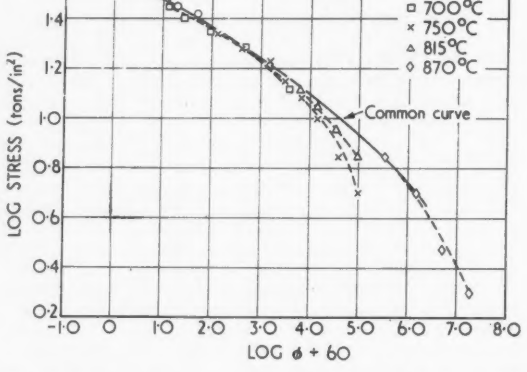


Fig. F—Life-to-fracture results on Nimonic 90 for 0·2% total plastic strain

the method. We have been able to select from our data two cases which are ideal to test the method, by using results obtained upon single casts of material which have been tested extensively at several temperatures. The use of these single sets of results eliminates much of the scatter seen in the authors' diagrams, as would be expected, and also discloses a limitation on the use of their parameters which has been concealed to some extent by this scatter.

Our first results were obtained on Nimonic 80*A*, and have largely been included by the authors in their paper, except that our values at the highest temperature were obtained more recently. Figure *E* shows the derivation of the authors' parameter ϕ plotted on axes log stress *v.* log $(\phi + 60)$. It incorporates life-to-fracture results at four temperatures—650°, 700°, 750°, and 815° C, and of durations between 100 and 34,000 h. Curves of similar shape are shown by the same creep tests for 0·2% total plastic strain.

For Nimonic 90, we have tests extending over a similar range of temperature, but with the addition of 870° C and up to a maximum time of 18,000 h. The parameter ϕ shows the same characteristics as for Nimonic 80*A*, both for fracture and for 0·2% plastic strain, those for the latter being shown in Fig. *F*.

In each case the points determined at any one temperature lie on a smooth curve which is concave to the origin. The points from the upper part of each of these curves lie fairly well on a single common curve, but the longer-time points at any one temperature fall away below the common curve.

A further point from our analysis is that we do not find the sudden change of slope in the region of log stress = 1·1–1·2 (i.e. at stresses of 12–16 tons/in²) upon which the authors place some emphasis. Our curves remain quite smooth through this range of stress, in which we have made tests at temperatures up to 750° C. In trials of various relations between stress and time, we have not found any evidence of discontinuities in this region. At higher temperatures we cannot comment, as our tests are at low stresses, but we certainly do not expect that a discontinuity would be unique to a particular stress level irrespective of temperature.

We have made useful applications of Mr. Graham's parameter when comparing casts of the same type of alloy in tests made at several temperatures for up to 5000 h, but our experience is that a more satisfactory method of presenting long-time creep data is to plot linear stress *v.* log time at each individual temperature level.

Figure *G* shows—upon such a basis—the results used

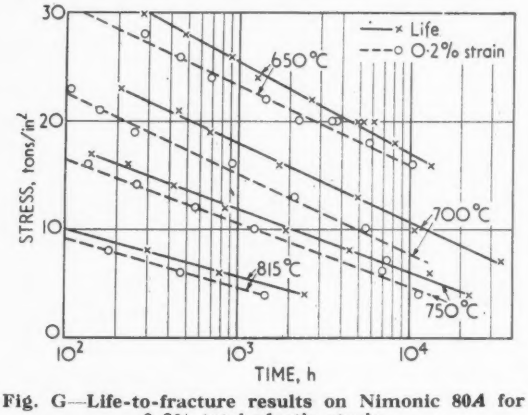


Fig. G—Life-to-fracture results on Nimonic 80*A* for 0·2% total plastic strain

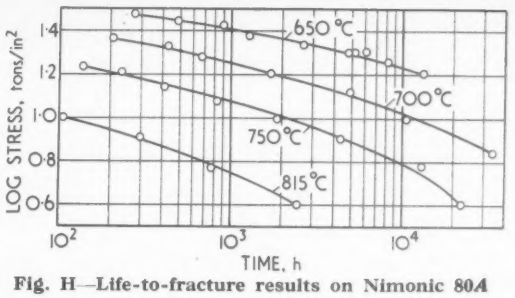


Fig. H—Life-to-fracture results on Nimonic 80*A*

for the common plot of Fig. *E* for Nimonic 80*A*. The tests at 700° C extend to 34,000 h and at 750° C to 22,000 h. For Nimonic 90, we have similar results at 650–870° C the maximum durations being lower; one test at 750° C and 7 tons/in² has reached 21,000 h and is still proceeding.

Straight lines give a very good fit for these two alloys at all temperatures examined. We have evidence from other work that this linearity does not extend to very high stresses and, perhaps somewhat arbitrarily, we take 100 h as the lower limit of the linear relationship. We also have evidence from other work that the lines will flatten at very low stresses. Tests are in progress at present on Nimonic 90 at the higher temperatures which have now proceeded far enough to show the start of this flattening.

Basically, the authors have adopted a plot of log stress *v.* log time, the complex term $(1175 - T)^{-22 \cdot 5}$ in their parameter ϕ being an adjusting constant for each temperature, to shift the log stress *v.* log time curves horizontally so as to join their upper parts together. Over the temperature range considered in Fig. *G*, and from 700° to 870° C for Nimonic 90, there is a trend for the slopes of the stress *v.* log time lines to decrease with increasing temperature. Consequently, when the same results are plotted on double logarithmic axes, as in Fig. *H*, they give curves which differ in shape, and which are unlikely to be fitted by any simple translation mechanism into a single curve.

We offer the suggestion, if it has not already been examined, that an application of the authors' approach, starting from the basis of stress *v.* log time instead of log stress *v.* log time, might devise a parameter which could both translate and alter the slope as temperature changes. This might result in a linear relation between stress and that parameter which could safely be extrapolated to very long durations.

To illustrate the limitation upon the authors' method, a prediction by means of common curves such as that shown in Fig. *E* calculates the life for Nimonic 80*A* at 7 tons/in² and 700° C to be 200,000 h, whereas the experimental result was only 34,000 h. At 4 tons/in² and 750° C the prediction gives 100,000 h and the experiment 22,650 h. In terms of stress, to give the actual lives observed, at 700° C the prediction would be 2·2 tons/in², and at 750° C, 2·4 tons/in² above the stresses applied experimentally. These differences are large enough to necessitate caution in applying the method to prediction of long-time behaviour.

As a matter of comparison, we have also applied the Larson-Miller analysis to the same results and have found a generally similar effect. At each temperature level, an individual curve was obtained, the upper parts of which could be blended into a single smooth curve but the long-time results again fell away. The life predicted by this method for 7 tons/in² and 700° C was 556,000 h and for 4 tons/in² and 750° C, 92,000 h.

Mr. **H. W. Kirkby** (Brown-Firth Research Labora-

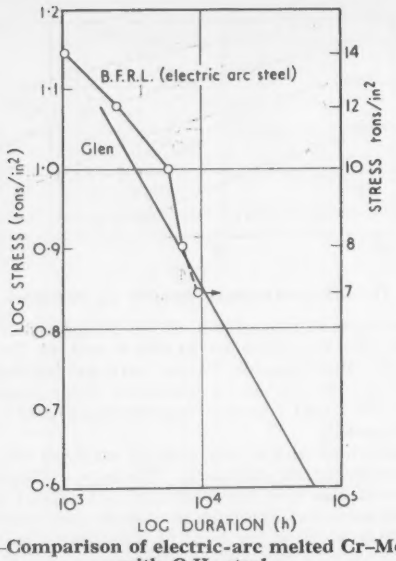


Fig. J—Comparison of electric-arc melted Cr–Mo steel with O.H. steel

tories): Mr. Glen refers to the poor creep properties of electric chrome–molybdenum steel and, whilst he qualifies this to some extent by referring to an 18-lb H.F. cast, the term ' electric ' only is used in various places. As shown in Fig. *J*, electric-arc melted Cr–Mo steel is compared with Mr. Glen's results for open-hearth steel; the comparison is favourable, and I have found that electric-arc melted steel has creep properties at least as good as open-hearth steel. It is our experience with Mo–V steel that small H.F. casts have relatively poor creep properties compared with Mr. Glen's open-hearth material, but commercial electric-arc casts have creep properties similar to open-hearth steel.

An interesting feature of Fig. *J* is the change of slope for the electric-arc steel, which is not an uncommon feature with a number of steels, though little has been published on this point. It is not possible to tell, however, whether Mr. Glen's curves show this phenomenon.

Figure *K* shows some rupture data for 2¼% Cr–Mo steel and the sharp change in shape is even more marked. This type of behaviour often accounts for optimistic extrapolations.

Mr. Glen's Fig. 12 compares American Timken data for 1% Cr–Mo steel with his own extrapolated 100,000-h data. Figure *L* compares the latter with the curve published by a joint committee of the A.S.T.M.–A.S.M.E.[2] which represents American ' official ' values for this steel. This latter curve is derived from rupture tests extending to 3000–4000 h, but the results compare favourably with Mr. Glen's.

Mr. Glen also refers to the important effect of tempering and how it influences a particular criterion. Figure *M* shows two curves plotted log stress *v.* log time for Mo–V steel with 0·18% carbon. The curves represent 3 and 16 h tempering at 690° C respectively and confirm Mr. Glen's findings that the longer tempering time benefits the 0·1% creep limit at the lower stresses. However, in terms of rupture strength, the reverse is often the case and I am surprised that Mr. Glen is finding that the normalized and tempered Mo–V steel has better long-time rupture strength than the normalized counterpart.

In the case of forgings where the creep limit is often set at 0·1% max. and the stresses are low, it is most important that sufficient tempering is given, otherwise the amount of primary creep may be high.

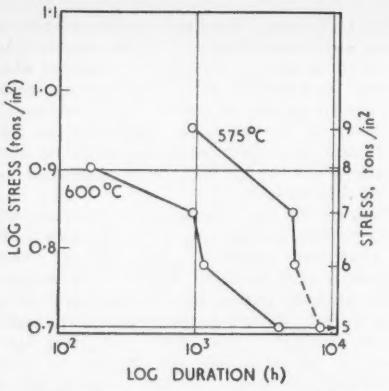


Fig. K—Rupture data for 2¼% Cr–Mo steel

Figure *N* illustrates some creep results obtained on a Mo–V turbine shaft with 0·2% carbon for 538° and 550° C. The change in the slope is an interesting feature of these curves and makes it clear how misleading extrapolation can be using short-time results.

Mr. **J. Glen**: The paper by Bardgett and Gemmill approaches so tantalizingly near a solution of this pensionable problem of the effect of aluminium on creep resistance. For this reason, I hope that the authors will continue their work in the hope of a final answer.

It has been clearly demonstrated in this paper that aluminium, as such, is not responsible for the variation in creep behaviour. The authors suggest that 0·015% soluble aluminium is critical because this amount is necessary to ensure the presence of enough aluminium nitride to cause an appreciable reduction in creep properties. Surely this is putting the cart before the horse. To me all the results in the paper indicate that nitrogen alone is the critical factor and that the effect of aluminium is simply to remove it from the system as insoluble aluminium nitride. In other words, nitrogen is a necessary constituent in ordinary mild steel to obtain good creep properties.

Further proof of this can be taken from the evidence of high-temperature tensile tests. In ordinary mild steel a peak is obtained in the maximum stress at about 200° C. A similar steel deoxidized with aluminium shows no peak in the stress when tested in the normalized condition and of course it has poor creep properties. I do not think anyone would suggest that the presence of aluminium nitride has reduced the tensile stress.

However, if the aluminium-killed steel is air-cooled

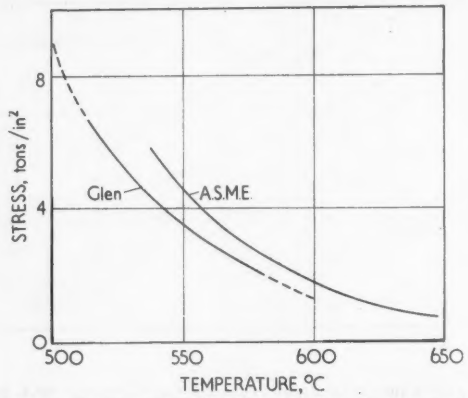


Fig. L—Rupture in 1000,000 h for 1% Cr–Mo steel

from about 1100° C the aluminium nitride goes into solution, and the steel then behaves as if none was present. A peak is obtained in the tensile stress and the creep properties are as good as those of ordinary mild steel. Slow cooling from 1100° C allows the aluminium nitride to form again. A very coarse structure is obtained. Again the tensile stress shows no maximum and the creep properties are bad despite the coarse structure.

Since it is unreasonable to suggest that the presence of aluminium nitride causes the reduction in tensile strength, it seems obvious to conclude that aluminium nitride as such is not important even for creep. In both cases it is the presence or absence of nitrogen which is important. To prove this conclusively, it would be necessary to make a steel containing no nitrogen, everything else being the same. I would predict that a steel made in this way would be abnormal, whether Al was added or not. Perhaps the N.P.L. might prepare such a steel from pure iron and discover whether this is so.

Although all the steels tested were made by the basic open-hearth process, it is reasonable to suggest that the nitrogen content would vary to some extent. Unfortunately, no mention is made in the paper of the nitrogen content of the various casts; have any determinations been made ? If the total nitrogen is accurately known and the amount present as aluminium nitride is subtracted, the final amount might correlate better with the creep properties and some of the slight anomalies which the authors mention might be explained.

Since all the work mentioned in the paper refers exclusively to basic open-hearth steel, there is also the possibility that the authors' results may have been more variable if steels had been tested where the nitrogen had been deliberately increased. Alternatively, some Bessemer steels could have been used. Perhaps the authors have some information on this point.

I might also mention the statement in the conclusion of the paper that the controlling factor is aluminium nitride by virtue of its influence upon the grain size. I am not quite clear what is meant by this since aluminium-killed steel can be made with a very coarse structure (i.e. slowly cooled from 1100° C) or a very fine structure (i.e. normalized 920° C), and in both cases the creep properties are poor. Here again, if the presence or absence of nitrogen is considered the grain size becomes of little importance.

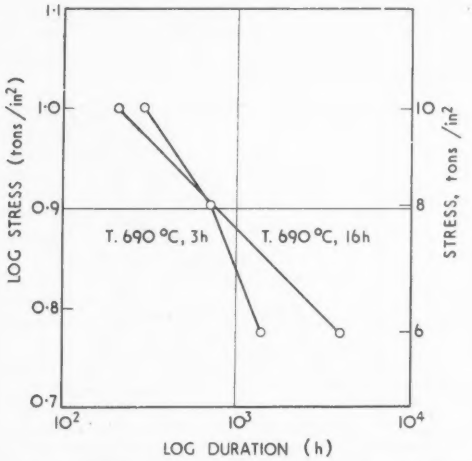


Fig. M—Log stress *v.* log time for Mo–V steel with 0·18% carbon, O.Q. 950° C, tempered as shown, 0·1% deformation

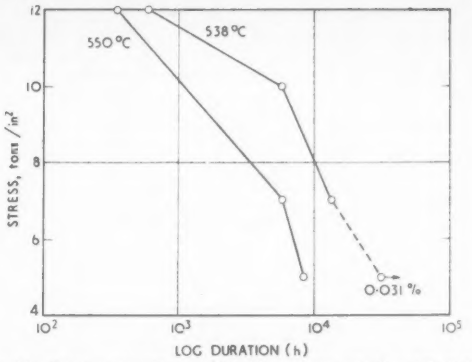


Fig. N—Creep results on Mo–V turbine shaft with 0·2% carbon

CORRESPONDENCE

Mr. **W. C. Leslie** (United States Steel Corp.) wrote : This paper represents a large amount of work and is a valuable contribution to our knowledge of the effects of deoxidation practice on mechanical properties of low-carbon steels. We are interested in this subject and I should like to ask the authors whether the observed deterioration of creep properties with increasing aluminum nitride is due to the presence of AlN, *per se*, to the removal of nitrogen from solid solution, or to variations in ferrite grain size ; and whether the steels were analysed for total ' acid-soluble ' nitrogen as well as for AlN nitrogen.

In this connection, it should be noted that aluminum-killed low-carbon steels can pick up nitrogen from the atmosphere during heat-treatment unless suitable precautions are taken to prevent it. Data on ferrite grain size for all of the steels would also aid in the interpretation of the authors' data and would be a valuable addition to the paper.

Dr. **C. H. M. Jenkins** (National Physical Laboratory) wrote: The possibility and the existence of AlN in an aluminium-killed basic open-hearth steel has been considered previously and we have already suggested[7] that its presence might be one of the causes of abnormality in this grade of carbon steel.

The fourth report[8] of the Oxygen Sub-Committee suggests that AlN was present in the insoluble residue after solution of the main part of the steel in an alcoholic iodine solution. In a steel containing 0·28–0·30% carbon and 0·14–0·17% aluminium which had been prepared in a small H.F. furnace with a sillimanite lining, 50% or more of the nitrogen could be traced as AlN in the residue after extraction. It was known by the method used that some attack of the nitride by the alcoholic iodine had occurred ; on the other hand, there was no strong indication that Al_4C_3 had been present, and at the most about 3% of Al_4C_3 had been traced in the amount of the alumina residue. The third report[9] suggested that Al C_3 can be formed in the case of a pure high-carbon iron alloy (0·70% carbon) melted in an alumina crucible in a H.F. furnace. The conditions could not be claimed to be those of equilibrium but alumina representing 0·23% Al_4C_3 was found in the metal. At that date identification of the constituents of a residue by X-ray methods had not been established.

From unpublished calculations of thermodynamical data by Dr. O. Kubaschewski at the National Physical Laboratory, it appears that AlN would be expected and would be formed (however slowly) at room temperature ; it would probably be dissolved at 1100° C in the austenite present. The data suggest that the aluminium nitride would be precipitated below 900–1000° C. On

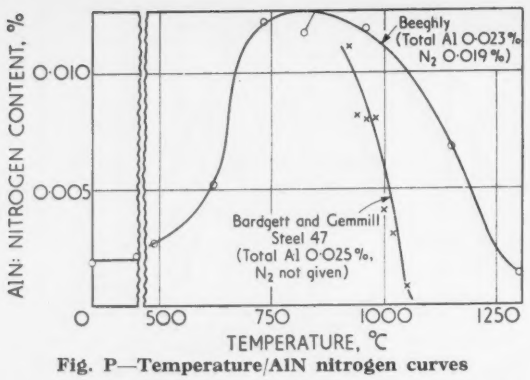


Fig. P—Temperature/AlN nitrogen curves

the other hand, the available data did not point to the formation of stable Al_4C_3.

The possible influence of oxygen, other than as soluble Al_2O_3, of Al_4C_3, of silicon and manganese, should be considered, as the high creep resistance of certain acid open-hearth steels and of a lesser number of basic open-hearth steels (made in a different furnace from that used by the authors) has yet to be explained; and it is by no means clear that these steels have any appreciable quantity of nitrogen in solution. Is it the nitrogen which is held in solid solution that imparts creep resistance in basic open-hearth steels and has this effect a counterpart in acid open-hearth steels? It should also be noted that the requirements of B.S.1271 for creep performance are relaxed enough to allow relatively cheap steels to be produced without difficulty, but carbon steels with a creep rate of only one-tenth of that demanded by the Specification can be produced by the acid open-hearth process and in a limited number of basic open-hearth melts. This is important if the maximum use is to be made of carbon steels as the rate of spheroidization, i.e. stability, bears a relationship to the resistance to deformation in creep.

We should give particular credit to Beeghly[10] for his research on an improvement in estimating the AlN content by collecting the residues after extraction by bromine in methyl acetate, and also to his work in tracing the effect of heat-treatment in causing the re-solution of AlN in austenite with increasing heat-treatment temperatures to 1200° C or more. The carbon steel used contained 0·017% N and 0·17% aluminium. Beeghly did not offer any explanation of creep behaviour.

The data contained in the present paper have been plotted (Figs. *P* and *Q*) and compared, in Fig. *P* with those of Beeghly and in Fig. *Q* with the creep data for three steels HKE, LPS, and a silicon-killed basic open-hearth steel JCN (steels 93, 84, and 94 respectively). The last-named results for the re-treatment of steels coarsened at 1100° C are given in Table VII of the paper by Jenkins and Tapsell.[7] The relative stability of steel JCN on re-treatment is clear. The similarity of the shape of the curves for the effect of reheating to various temperatures in the range 500–1250° C for both the aluminium nitride content and the resulting creep rate points to a connection between creep rate and aluminium nitride content in this class of steel, but the evidence is not conclusive in explaining the reasons for good creep resistance of high-quality acid open-hearth carbon steels. An explanation based wholly on AlN being in solid solution seems inadequate. The results of the authors' work relate to commercial quality basic open-hearth steels but are nevertheless most helpful.

Mr. **M. G. Gemmill** wrote: : Mr. Glen has performed a most valuable service to users of low-alloy creep-resisting steels in making available further data on ½% Mo, 1% Cr ½% Mo, and ½% Mo–V steels. This is not the whole of his achievement. He has also ingeniously related this information to its ultimate use in providing design stresses. Among the deductions made, he has been particularly concerned to examine the 'thermal action' effect, and whilst he has succeeded in correlating observed creep resistance with heat-treatment, test conditions, and possible carbide behaviour he also seems to imply that a practically useful prediction of long-time creep strength from comparatively short-time tests will always remain an unattainable ideal. Would he consider that investigation of the types of carbides present in these steels, linked with a knowledge of their form and dispersion, might lead to a more direct solution to this problem than is obtained by detailed analyses of creep results ?

In discussing the effect of normalizing temperature, the author refers to the finer grain exhibited by an electric-furnace steel compared with open-hearth steel for any one normalizing temperature. It would be interesting if he could explain this. It seems that this behaviour refers to small H.F. casts. Would the author expect similar characteristics to be shown by steel made by the electric-arc process ?

Referring to the important question of ductility at high temperatures, Mr. Glen sub-divides intercrystalline failures into those occurring at the vicinity of fracture (1% Cr, ½% Mo and ½% Mo–V in the normalized and tempered condition), and those showing this feature along the whole gauge length (½% Mo and ½% Mo–V in the normalized condition). This behaviour might imply an inherent 'stiffness' in a ½% Mo ferrite, removable either by tempering or by further alloying. Has the author any data upon the rupture ductility of ½% Mo steel in the normalized and tempered condition? He has, of course, mentioned the different behaviour of the 1% Cr, ½% Mo steel, and it is pertinent to mention that a 2¼% Cr, 1% Mo steel tested for times up to 25,000 h has only exhibited ductile, transcrystalline fractures. Comparing the rupture ductility of the 1% Cr, ½% Mo and ½% Mo–V steels I wonder whether this property of the latter steel would not be detrimentally affected by larger section sizes due to the sluggish behaviour of vanadium as an alloying element. It would be interesting to have the author's views upon this effect of Cr additions to the ½% Mo–V steel.

Finally, the author shows that rupture ductility passes through a minimum (Figs. 17, 18, and 19) ; would this not mean that the iso-strain and rupture lines would ultimately diverge and not be quite of the shapes suggested in Figs. 8, 11, and 14 ?

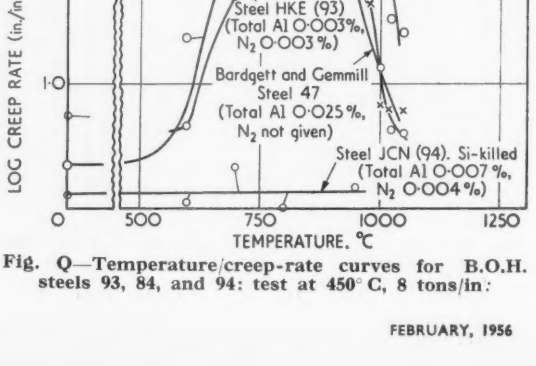


Fig. Q—Temperature/creep-rate curves for B.O.H. steels 93, 84, and 94: test at 450° C, 8 tons/in:

Dr. **R. W. Bailey** wrote: Probably most people who have taken more than a passing interest in the phenomenon of creep, especially when interest has been in long-time service applications, have given attention to ways and means of interpreting or predicting from comparatively short-time tests, long-time behaviour, if the procedure favoured is to have general application. The present paper, in being virtually confined to relationships and properties "of a commercial alloy," namely Nimonic 80, is not of this generality, and so important limitation may be suffered if it does not happen to have a property clearly recognizable, which can be taken into account by the general analysis used. This is particularly the case if the property or feature depends upon the influence of thermal action, not only in altering the rate, such as would conform with a term of the kind $Ae^{-Q/RT}$, but also which alters the character of the feature, whereby A is no longer a constant, but would itself be a function temperature T and time t.

Evidence of tests show that such a feature is likely to be individual to the material and, if so, generalization would be precluded. If this does not happen to be the case for Nimonic 80, relationships embodying the influence of temperature would conform with many that have been suggested and found wanting for general use.

Failure to recognize the significance of this fact invalidates the general deductions made by Larson and Miller, on which Graham and Walles's equation (2) is based. This is only true if A is constant. Otherwise A would need to be replaced by functions of T and t which would be individual to the material represented. A more complete and detailed knowledge of A would probably in some cases show it also to embody a function of the stress f; but primarily it would be a function of T and t, especially when f was in the practicable range of working stress. In as much, therefore, as equation (2) enters into the relations developed by the authors, such relations must have their general character limited. The writer gave some attention to this matter in a recent paper[11] and chose an empirical form and procedure in his Figs. 13 and 14 to take the variation of A into account. Incidentally, the method suggested provided an explanation of the change of slope or the two slopes which, according to U.S. test results, appear to characterize the log f/log t diagram, where t is the time to rupture by creep under a stress f.

AUTHORS' REPLIES

Mr. **W. E. Bardgett** and Mr. **M. G. Gemmill** (United Steel Companies): It is interesting to have Mr. Ward's confirmation of the correlation between AlN content and austenitic grain size. We would not consider the duplicate results for steel 28 (Table III) to be significant in relation to the general conclusions drawn from the work as a whole; the duplicate tests on steel 32 were taken from the same length of bar and are viewed, as stated in the paper, to be of interest in showing the wide variation in creep resistance possible in a mixed-grain steel. The duplicate tests on steel 30 represent specimens taken at random from different billets; whilst it is not known whether they represent different ingots it is probable that they do, the differences in soluble aluminium being affected by the aluminium mould additions made to compensate for oxidation losses of aluminium during teeming.

The good creep resistance of acid O.H. steel, referred to by Dr. Jenkins, may, of course, lend weight to the view that in these steels having no aluminium addition it is the nitrogen thus left in solid solution which confers creep strength.

In connection with Mr. Leslie's contribution, the ferrite grain size has only been determined for the series of twelve steels having an aluminium addition of $1\frac{7}{8}$ lb/ton; this grain size does not appear to bear any clear relation to the soluble aluminium contents of these steels. Mr. Glen mentions the differing behaviour in the maximum stress values at 200° C of mild steel with and without aluminium as a deoxidant. Possibly the absence of a peak at 200° C in the aluminium-killed steel can be directly attributed to the AlN which, by locking up the nitrogen, removes the strain-ageing effect causing the peak.

Mr. Glen, Mr. Leslie, and Dr. Jenkins have raised the general question of the mechanism accounting for the variation in creep behaviour. When only one steelmaking process is being examined the nitrogen content will be, relative to the aluminium content of these steels, a relatively constant factor. For this reason it is not possible to distinguish between nitrogen as a solid solution hardening element and the effect of aluminium nitride itself in controlling the creep behaviour. Mr. Glen suggests that the role of nitrogen is of primary importance but, even if this is proved to be the case, it is aluminium which is practically important in removing the nitrogen from solid solution.

'Acid soluble' nitrogen analyses are only available for steels 21, 30, 47, and 52, four of the five steels included in Table V of the paper, and are given below. It is clear that an attempt should be made to discover, as Mr. Leslie suggests, whether AlN acts to reduce creep resistance on its own account or by virtue of nitrogen removal from solid solution. Tests are now being put in hand on a series of steels having three levels of nitrogen content and two levels of soluble aluminium content to answer this point.

Steel No.	Heat-Treatment	Nitrogen, %	
		Soluble	Insoluble
21	920° C, A.C.	0·004	Nil
30	,,	$0{\cdot}004_5$	0·001
47	,,	$0{\cdot}004_5$	0·001
52	,,	$0{\cdot}003_5$	$0{\cdot}000_5$

Mr. **J. Glen** (Colvilles Ltd.): Before replying to the discussion I would like to add one result to those already given in the paper. A rupture test on normalized and tempered Mo–V steel was reported as unbroken after 135,000 h of test at 550° C and 6 tons/in². This test has now broken at 139,000 h with an elongation of 7%. The time to fracture is somewhat greater than that predicted by a straight-line extrapolation, but the elongation at fracture is in good agreement with the curve of Fig. 18b of the paper.

Mr. Ward is interested in the comparison between creep strength and other mechanical properties and gives an example where large changes in creep resistance occur with no significant change in the tensile properties. All the work I have carried out suggests that if the creep resistance is altered, by tempering for example, some change will also occur in the tensile properties. This change may be small but is surely significant. Mr. Ward might find that a difference in properties is clearly shown by a true stress/strain tensile test, whereas it may be masked by using the conventional tensile strength as a basis of comparison.

Mr. Kirkby presents convincing evidence to show that

an electric furnace steel has properties as good as those of a steel made by the O.H. process. He agrees, however, that steel made in an 18-lb H.F. furnace has a relatively poor creep resistance. I would like to correct any impression that I may have made that electric furnace steel is necessarily inferior to O.H. material; but it does seem that the grain-coarsening characteristics of creep steels are important and worthy of further study since the grain size appears to have a fundamental effect, particularly on low-deformation creep.

Regarding Fig. 12, I was aware that the official U.S. values for the rupture strength of Cr–Mo steel are fairly well in agreement with those quoted in the paper. The results included were intended to show the danger of extrapolating the results of comparatively short-time tests.

With regard to tempering, Mr. Kirkby is correct in stating that tempering, particularly long-time tempering, reduces the rupture strength compared with the normalized material. It may be that the rupture strength of the normalized Mo–V steel in 100,000 h is better than that after tempering for about 5 h at 690° C, but the results as far as they go seem to indicate the opposite.

Mr. Kirkby's interesting results clearly emphasize the dangers of extrapolation and the necessity for carrying out long-time tests.

In replying to Mr. Gemmill I would like to amend the implication I appear to have made that the prediction of long-time creep strength from comparatively short-time tests will always remain an unattainable ideal. We have a long way to go before such predictions will be possible, but if more were known about the form and dispersion of the alloying elements in steel and their effect on the creep process it should ultimately be possible to predict the long-time behaviour of the material from the results of a few tests.

From the few tests I have carried out on basic electric arc furnace steel I have found that on normalizing at, for example, 950° C, electric furnace steel has a finer structure than O.H. material. This is more particularly true of small H.F. furnace casts. As already indicated I would not like to suggest that this is true of all electric-furnace casts, but it does appear as if more work is necessary on the question of the effect of manufacturing variables.

With regard to high-temperature ductility, the few results I have obtained suggest that 0·5% Mo steel is improved by tempering but at the expense of rupture strength. It is interesting to note the high ductility obtained on $2\frac{1}{4}$% Cr, 1% Mo steel, and there appears to be no doubt that this results from the chromium addition. As indicated in the paper the Mo–V steels with a chromium addition of about 0·4% appear to have a somewhat better ductility than straight Mo–V steel.

The effect of large sections on the high-temperature ductility of Mo–V steel is not important, because the ductility is improved by the tempering treatment and should not be influenced by section size.

Finally, it may well be, as Mr. Gemmill suggests, that the iso-strain and rupture lines ultimately diverge, but until more direct evidence on this is available it will be necessary to extrapolate more or less as shown in the paper. It should be realized that when plotting results on a logarithmic time scale a very short distance on the graph at about 100,000 h represents quite an enormous time. For this reason the convergence of the curves is possibly more apparent than real.

Mr. **G. T. Harris** and Mr. **H. C. Child** (William Jessop and Sons Ltd.): Mr. Ward has mentioned the important point that if castings are to be used for rotor blades, ideally these must be designed to suit the material and the process. So far in the U.K. castings have been considered mainly as a direct replacement for wrought blades designed for the highest engine efficiency and of a shape more suited to the processing characteristics of wrought alloys. The designs have in general also imposed substantial fatigue stressing, and, owing to the undoubtedly poorer fatigue strength of castings, this has led to marked hesitation in changing to cast alloys. If the designer will accept castings in principle, and design his blades to keep fatigue stressing to a minimum and to facilitate the production of sound castings, good use may be made of the excellent high-temperature strength of cast alloys.

Undoubtedly castings can be produced with lower scatter than that reported in our paper, and indeed subsequent work in our own laboratory has achieved this; but the lesson to be learnt from these results is that the precision casting process can be controlled as far as reproducible high-temperature rupture strength is concerned. Indeed, in answer to Mr. Ward's query the scatter of G34 rupture strength is less than that which occurs in many wrought heat-resisting alloys.

We regret that chemical analyses on individual nests were not carried out, but as the scatter within a nest was as great as that between nests this omission has probably little practical importance. Our fatigue units were run at 5000 rev/min, well below the resonant vibration frequency.

We are in complete agreement with Mr. Ward that the properties of castings can be improved by perfecting the casting technique beyond the level of apparently sound castings, but such work if carried out statistically on high-temperature properties is of such magnitude as to deter most investigators.

Mr. **A. Graham** and Mr. **K. F. A. Walles** (N.G.T.E.): Replying to the discussion as a whole, we would not advocate the use of very short-time tests to estimate creep at very long times; we used short-time tests as the only practicable means of investigating the principles of extrapolation, and as a basis for moderate extrapolations. Nor would we expect that the results of an arbitrary short-time test performed and assessed regardless of factors mentioned in the paper would have any particular relation to long-time results. The different influences that occur must be carefully unravelled before extrapolation is considered.

In reply to Mr. J. O. Ward, we are glad to learn that some of his data join the widespread evidence supporting the Andrade equation and the Larson and Miller parameter. However, the contrasting fact, which is beyond dispute, that many data do not provide support, indicates that they should be used with caution. Evidently they are either approximations to, or special cases of, more general laws, and it is in this sense that we have used them. The inference to draw from the various successful examples of time/temperature co-ordination in complex materials is, we believe, that one can no longer maintain the impossibility of extrapolation because of the differing relative effects of temperature and time on the very many processes that operate. Apparently the number of processes that need to be distinguished is rather small.

On the question of extrapolation to within $\pm 5\%$ in stress, until the stress for given creep can be established by direct experiment to an accuracy appreciably greater than this, any form of mere extrapolation will evidently result in greater errors.

Mr. Overy has drawn attention to the interesting possibilities of various simple power laws involving rate of strain. Nutting[3] proposed the following differential form of his law

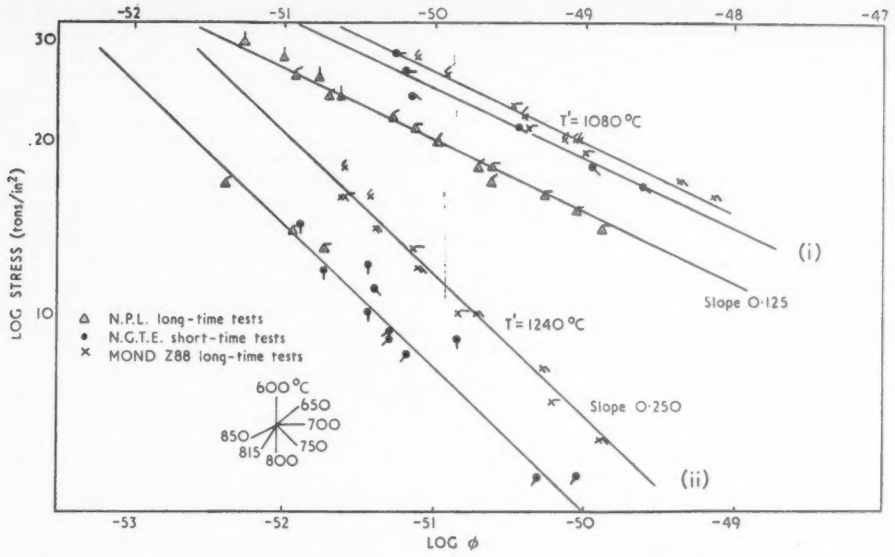


Fig. R—(i) and (ii) Comparison of N.P.L., N.G.T.E., and Mond tests on Nimonic 80*A* and 90

$$\frac{d\epsilon}{\epsilon} = \kappa \frac{dt}{t} + \beta \frac{d\sigma}{\sigma} + \nu \frac{dT}{T}$$

while Scott Blair and Reiner[4] differentiated the Nutting law with respect to time to obtain the equivalent form

$$\frac{\dot{\psi}}{\psi} = \beta \frac{\dot{\sigma}}{\sigma} - \frac{\dot{\epsilon}}{\epsilon} + \kappa \left(\frac{\sigma\beta}{2\psi}\right)^{\frac{1}{\kappa}}$$

Similar equations would result from differentiations with respect to stress and strain. Equations of this type appear very promising at first sight, especially if a time-temperature variable is used in place of the separate variables. For example, they reproduce, qualitatively, the peculiarly-shaped families of experimental curves that were obtained by Tietz, Anderson, and Dorn, and by Dorn, Goldberg, and Tietz, which are associated with sudden changes of stress or temperature during loading. However, a single known fact is sufficient to reject them as they predict incorrectly that all strain is instantaneously recovered upon removal of load. This is a good example of the advantage of attending to salient factors well outside the immediate problem under consideration. Equations of the above kind are differential equations of a σ, ϵ, t surface, and the surface is permanently fixed by the initial conditions of loading. The theory given in the paper is a preferred generalization in which the position of the surface changes from instant to instant of loading.

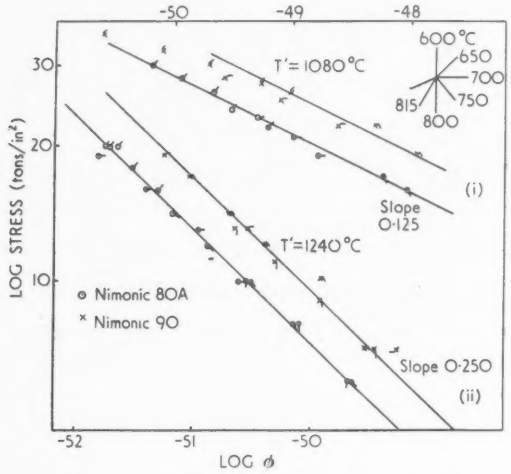


Fig. S—Log stress *v.* logϕ data for Nimonic 80*A* and 90

Before replying in detail to Mr. E. D. Ward's contribution, we should like to be sure that no misconception may arise from our work and from remarks on the materials made by his firm. The marked changes of slope to which we have drawn attention on log stress/log time–temperature graphs are, we believe, common to most highly-alloyed materials if examined over a sufficient course, in themselves relevant to the merit of materials range, and are probably universal. They are not, of from the user's point of view; merit depends upon the height of the curve at any time/temperature ordinate.

We are glad to have his confirmation of the validity of the modified Andrade equation and to learn that the parameter ϕ is of value in tests up to 5000 h.

He has contrasted the continuous downward curvature of his graphs of log stress *v.* log time at constant temperature with our proposal of linear segments with abrupt transitions, and suggests that the graphs are straight lines if stress is plotted rather than log stress. We believe it is not possible to decide these matters if attention is limited to a small range of materials and behaviour. Our view has arisen from the attempt to provide the widespread general co-ordination indicated in the paper. We have been led, for the reasons given, to represent creep by the sum of a number of power terms, and it is important to notice that such a sum, when plotted on log/log paper, will only in favourable circumstances (e.g. in Fig. 3 of the paper) give clearly distinguishable linear segments. In general, as in Fig. 4, it plots as a continuous curve. That a curved transition can be straightened by changing from log stress to stress appears to be an incidental fact that may be misleading. Sparse or scattered data may give an incorrect impression of a sharp transition because the best straight lines drawn

through each linear segment are continued for lack of guidance until they meet. The true situation is only revealed by complete analysis as in the examples given in the paper. The problem of understanding creep appears to be that of unambiguously identifying the individual terms of the general series referred to on p. 116.

Further evidence on this problem appears when long- and short-time data are compared. A distinction must be drawn between the full provisions of the theory and the extent to which it could be applied to the data considered in the paper. In its most general form, the expression for creep has, in principle, as indicated on p. 116, different constants A and T' in each of the ϕ_1, ϕ_2, that appear in the different terms. The available data were inadequate, however, to distinguish any differences, and common constants were therefore used in equation (17). In particular, the data were meagre in the lower stress range to which Mr. Ward's comments primarily refer. The excellent results obtained by Mr. Slimak now supply further evidence.

The time-temperature principle is that of superimposing the curves for different temperatures by parallel displacements along the log time axis, and the systematic trends of his data clearly show that different displacements, i.e. different constants A and/or T', are needed in the upper and lower ranges of stress.

Owing to limitations of space, attention must be confined to his rupture points for which it is sufficiently accurate to consider the upper and lower ranges mutually independent. For the Nimonic 80A data of his figures, values of A and T' for the two regions are found to be 20° and 1080° C and 20° and 1240° C respectively. Figures R(i) and (ii) show, for both Nimonic 80A and 90, the points for the two regions plotted separately with their respective time-temperature constants. The coordination of the results for different temperatures is seen to be close, particularly for Nimonic 80A. Use of the same constants for the data in Fig. 9 of the paper leads to Figs. S (i) and (ii) for upper and lower ranges respectively. The major part of the scatter in the short-time results in Fig. S (ii) is associated with a single temperature (800° C) and therefore is not relevant. As mentioned in the paper, the specimens were from different heats and the material was different from that to which the Mond Nickel points refer. There is not necessarily any disagreement on the present basis, therefore, between the short- and long-time results.

The above evidence is not enough to justify fully the proposed interpretation, but we draw attention to the fact that we have found the magnitudes of the slopes in the figures to be common to a number of materials. With different time-temperature constants for consecutive linear segments the stress at which the transition occurs will not be constant, but will depend upon both time and temperature.

We can only reply rather generally to Dr. Bailey's written contribution. Concerning the possibility that thermal action should be taken into account by means of a factor of the form $Ae^{-Q/RT}$ in which A is a function of both time and temperature, we are doubtful whether 'thermal action' should be regarded as an independent entity requiring independent consideration in the theory. Since a material after any period of testing is always different from that before testing on account of the simultaneous effects of temperature, stress, and time, it is far simpler to regard the regular features observed in creep as features of materials that are in fact in course of change by specific forms of 'thermal action.' A theory that describes these features directly will then take these particular forms of thermal action into account. A logical generalization of that theory may then include more general forms.

We abandoned a time-temperature parameter of the kind used by Larson and Miller for the reasons given in the paper, and we disagree with Dr. Bailey's objection to it. In view of Holloman and Jaffe's[5] introduction of this parameter for the tempering of steel, and of Larson and Salmas'[6] justification of it for grain-size and hardness in a copper-based alloy, it must presumably be regarded, not as ignoring thermal action, but as an inadequate expression of thermal action.

Although our experimental work was limited to Nimonic 80 and 80A, theoretical attention was given, as the Appendix shows, to many forms of behaviour common to a wide range of materials. A limitation of the observed behaviour to the Nimonic alloys is not therefore to be expected, and we have recently obtained direct evidence of similar behaviour in other high-temperature alloys. The constant A in the time/temperature parameter ϕ appears to remain unchanged over these alloys.

We have not had an opportunity to study the specific problems considered by Dr. Bailey in his paper.[1] We did not base our work on an equation of the form used there for the reason given in our reply to Mr. Overy. Both theories are based upon power laws, but another distinction between them is worth recording. Dr. Bailey's equation may be compared with n single term of our formulæ, but with exponents that vary with stress and temperature ; we, on the other hand, use a number of terms in which the several exponents remain constant. We are following up the possibility which, we believe, will eventually lead to the simpler mathematics.

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DISCUSSION ON VANADIUM PENTOXIDE ATTACK

This discussion was based on the following papers (the dates of publication in the *Journal* are given in parentheses) :

" The Liquidus of Metal-Oxide/V_2O_5 Systems and the Mechanism of Accelerated Attack on Metals by V_2O_5," *by G. Lucas, M. Weddell, and A. Preece* (1955, vol. 179, April, pp. 342–347).

" Effect of the Composition of Gas-Turbine Alloys on Resistance to Scaling and to Vanadium Pentoxide Attack," *by G. T. Harris, H. C. Child, and J. A. Kerr* (1955, vol. 179, March, pp. 241–248).

The papers were presented by Dr. **G. Lucas** (Brown-Firth Research Laboratories) and Mr. **H. C. Child** (William Jessop and Sons, Ltd.) respectively. Mr. Child pointed out that in Fig. 9*b* of his paper, the value of 7·3 inside the graph should have read 73.

Mr. **H. T. Shirley** (Brown-Firth Research Laboratories): Until recent years the vanadium problem was largely concerned with consumable items like superheater supports. With the advent of the gas turbine the problem has become much more serious. We now have engineering parts which must be kept within reasonably fine limits, and the vanadium problem is manifest. It is not simply vanadium pentoxide : other combustion products, gaseous and solid, may greatly complicate the picture and alter the emphasis of the vanadium attack.

In their paper, Harris, Child, and Kerr have shown caution in their final deductions. This is most necessary when attempting to translate laboratory data on this subject into practical advice to engineers. Careful correlation with practical experience is essential for confidence. Where trouble occurs in practice, laboratory tests can be most useful in helping to sort out dominant factors ; but the process is not so valuable in reverse. There have already been considerable surprises in service behaviour and we must expect others.

On p. 241, there is an indication of a top practical blade temperature of 550° C for residual-oil working in gas turbines. Surely that is too pessimistic. Reasonably satisfactory working has been achieved at 600° C or a little higher, but it serves to emphasize the gap of 200–300° C between such temperatures and those at which many of the tests in the paper have been carried out. Moreover, the basic comparison rate of 1 mg/cm²/h of Table III would mean a very short life for close-tolerance parts. Special care is necessary in relating the data to present service conditions.

The areas of low resistance in Figs. 8 and 9 are intriguing, and I should be very interested to hear an explanation. I should also like to know whether the authors think that such an effect would exist at 650° C. In connection with molybdenum-bearing steels, it should be pointed out that the original Leslie and Fontana work was with 16/25/6 Cr–Ni–Mo steel in still air at 900° C. It would be interesting to know whether the authors have any evidence of trouble with steels under conditions more applicable to present gas-turbine practice, for which the molybdenum effect is a subject for no great anxiety.

The data on melting points provided by Lucas, Weddle, and Preece are most interesting, even though the immediate practical importance of this aspect may now seem less than when the work was started. It has in fact become evident that additions capable of raising the melting point of the vanadium deposits by no means ensure the desired improvement. Omitting the engineering difficulties of effecting the necessary additions, various factors, including chemical stability in the presence of sulphur gases, introduce damaging complications. In my experience aluminium oxide is disappointing as an inhibitor ; calcium and magnesium oxides are only partially successful because of sulphation ; zinc has a less stable sulphate but is not so satisfactory an inhibitor as magnesium ; and from the present paper you will see how diffusion of vanadium oxide through chromium oxide spoils any protective properties it might otherwise have had. This work on chromium oxide is one of the most interesting features of the paper, but in spite of its disappointing behaviour here, we have been surprised recently at the exceptional resistance shown by a 26% Cr steel in practice.

The subject is obviously full of complications, often requiring quite involved testing, but it is still very important to know what V_2O_5 itself will do ; and that is where the authors of these papers have served us well.

Mr. **G. Burns** (Royal Naval Scientific Service) : The method of determining the oxidation rate used by the authors of the second paper has shown the very marked increase in oxidation rate which occurs in heat-resisting alloys above certain temperatures, but it does not show a curious feature of the oxidation of *G.18B* and some other high-temperature alloys when they exceed a certain temperature.

A certain amount of work has been done at the Admiralty Materials Laboratory in which cylindrical specimens of the material, 1·4 cm dia. × 0·9 cm, were placed in silica or alundum thimbles suspended from one arm of a balance into a vertical tube furnace, through which we restricted the air supply as much as possible so that the conditions were not disturbed, and continuously weighed during the test of 1000 h.

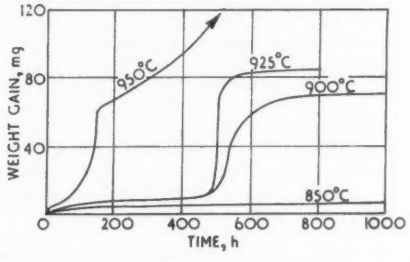

Fig. A—Weight-gain/time curves: 850–950° C

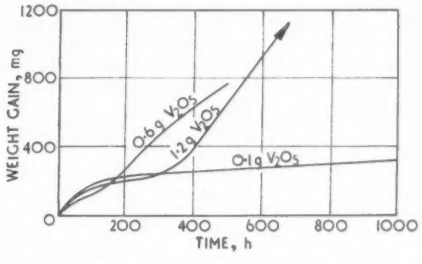

Fig. B—Weight-gain/time curves: 750° C

At temperatures up to 850° C the weight-gain/time curves (Fig. *A*) over the whole course of the tests were as would be expected from the formation of an adherent protective scale. At 900° C the weight-gain/time curve followed the same course for nearly 500 h, but then showed a very rapid acceleration for the next 100 h, after which the rate of oxidation slowed down again, and continued to be fairly slow up to 1000 h, when the test was stopped. At 925° C the same effects appeared, but more strikingly. By the time 1000 h was reached the weight-gain had almost ceased. The test was continued for another 300 h, making a continuous period of 1300 h altogether and during that time there was no perceptible weight-gain. We found that weighings to ½ mg were not difficult, so that we can call it negligible.

At 950° C acceleration of the rate of oxidation occurred after about 60 h. There was some deceleration between 150 and 200 h, followed by a slower and gradually increasing acceleration, which continued until the test was stopped at 950 h.

In tests of the behaviour of the alloy in contact with large amounts of vanadium pentoxide at 750° C similar weight-gain/time curves were obtained. The vanadium pentoxide was placed in the thimble with the specimen at the beginning of the test, and no further additions were made. The time and the weight-gain at which acceleration occurred seemed related to the amount of vanadium pentoxide in contact with the sample.

Figure *B* shows what happened in one or two of those tests. The first test was with about 1·2 g, which is a vast amount of vanadium pentoxide. The sample started to form a protective scale and then accelerated and continued to do so. The test ran for 3000 h, at which stage the weight-gain was 3 g, and there was an enormous amount of scale. We thought that the inflexion, occurring where it did, was probably related to the composition of the scale or the proportion of scale and vanadium pentoxide. So another test was done with half the amount of vanadium pentoxide, and in both time and weight-gain the inflexion occurred earlier. Finally, a test was done with 0·1 g V_2O_5. Then we almost certainly missed the inflexion, but the curve is more rapid in the early stages than either of the other two.

It was tempting at first sight to suppose that the vanadium pentoxide was causing the alloy to show at 750° C a behaviour which it would in any case have shown at a higher temperature without vanadium pentoxide. However, we carried out some tests with other alloys, and some which behaved in exactly the same manner as *G*.18*B* when exposed in air at 900° C did not show this behaviour at 750° in contact with V_2O_5. So evidently that tempting idea has to be rejected.

Professor Preece and his co-workers show clearly that the oxidation of chromium at 700° C is accelerated by contact with vanadium pentoxide, and deduce from this that the use of chromium as an element conferring oxidation resistance is ineffective in the presence of this substance. This seems reckless when considered in the light of the results given in the second paper which indicate that, though chromium does not eliminate accelerated oxidation in the presence of vanadium pentoxide, it does effect some reduction. This is supported by the results of some tests at the Admiralty Materials Laboratory which showed that an alloy containing 20% of chromium was substantially more resistant to oxidation in the presence of V_2O_5 than alloys containing only 14% Cr.

It may be worth mentioning the results of analyses of the scale formed in the test in which we had finally about 3 g weight-gain and a massive lump of scale. The scale was separated into a number of layers, and each layer was analysed separately. Unfortunately, the results varied in an irregular way. However, we took the quantities of iron, nickel, cobalt, chromium, and molybdenum present as a proportion of the total of those metals in the sample, and we found that the chromium and cobalt contents of the scale were always much less than in the original alloy ; the contents of nickel and molybdenum in the scale were generally higher than in the original alloy; and the iron content was much higher.

Professor Preece and his co-workers limited the temperature of their experiment with chromium to 700° C to restrict the volatility of the vanadium pentoxide. When we were making tests at the Admiralty Materials Laboratory, a sample of vanadium pentoxide was heated several times to 950° C. On the first heating and cooling cycle, the sample lost about 0·2 wt.-%, but in subsequent cycles, although there were curious irregularities in the weight during the cycle, no further loss in weight took place over some five or six further cycles heating to 950° C. Professor Preece and his co-workers might have carried out their tests at a higher temperature.

Mr. K. Sachs (G.K.N. Research Laboratories) : The destruction of materials exposed to ashes rich in vanadium pentoxide at temperatures above its melting point is a very serious problem. No metallurgical solution has yet been found, and one rarely reads a paper on this subject by metallurgists which does not suggest that the oil industry would earn our undying gratitude if they could find an additive for their residual oils which would combine with vanadium pentoxide and render it harmless. Unfortunately, oil technologists have not exactly fallen over themselves to produce a suitable miracle pill, and in the first paper we see an attempt to find a substance which might form the basis of an oil additive.

At present there is no alloy which can withstand vanadium pentoxide attack and no method of making the oil-ash harmless. It is necessary, therefore, to assess the degree of deterioration of various existing alloys. The second paper is a very valuable contribution to the growing body of information on this subject.

It is interesting to compare their results with those of a similar investigation[1] into the effect of contamination with pure vanadium pentoxide and with various mixtures of vanadium pentoxide and sodium sulphate, on the scaling resistance and stress-rupture life of alloys used in gas turbines. Our grading of the alloys in order of resistance to V_2O_5 attack was roughly similar to that indicated in the second paper. Comparing the life to rupture of test bars carrying a coating of contaminant with that of clean bars, it was found that in alloys with 12–20% of chromium there was a continuous gradual improvement as iron was replaced by nickel. I must add that the steps were fairly broad, and we did not deal particularly with the narrow gap round about 25% of nickel which has been found to be particularly susceptible to V_2O_5 attack. We found that cobalt additions seemed to have rather peculiar effects. High cobalt contents (about 50%) behaved rather similarly to equivalent amounts of iron, while low additions (about 10–20%) gave shorter lives than were found in any cobalt-free alloys. This can be partially accounted for by the somewhat longer lives of the clean bars of cobalt-containing alloys ; but there were examples, particularly in the case of nickel-based alloys, where the life of the cobalt-free alloy was reduced from 300 to 220 h, and that of an alloy containing 20% of cobalt was reduced from 270 to 73 h. Nothing could be seen in the structure after exposure at 750° C (the temperature at which the tests were carried out), and in order to see what would happen under extreme conditions we carried out a few pilot experiments, heating samples of the alloys at 1000° C. Iron-based alloys up to about 50% Fe gave a spongy scale and a relatively smooth surface with rapid removal of the metal, while at higher Ni contents there was a little

intergranular penetration in advance of the main scale. It was significant that in the case of alloys with 20% Co this intergranular penetration at 1000° C was much deeper and more pronounced than with Co-free alloys.

The experiments of Harris, Child, and Kerr were carried out with relatively small quantities of vanadium pentoxide. Comparison of their Figs. 5 and 6 shows that the base composition (40% Fe, 20% Ni, 10% Co, 20% Cr), with and without varying additions of Nb, W, Mo, and Ti, is more severely scaled in 24 h at 850° C in the presence of V_2O_5 than in 70 h at 950° C in its absence; alloys containing vanadium are oxidized more rapidly than the other alloys in both experiments, and heating at 950° C for 70 h has more effect than a 24-h treatment at 850° C in the presence of a little vanadium pentoxide. The progressive increase in scaling index with vanadium content of the alloy suggests that it is due to the increase in the amount of vanadium pentoxide in the scale. Clearly the treatment at 950° C will lead to the formation of greater amounts of vanadium pentoxide than the shorter treatment at a lower temperature, and the amount of vanadium pentoxide deliberately added in the 850° C treatment was considerable less than that formed by the oxidation of the vanadium-bearing alloys. If larger amounts of vanadium pentoxide had been used in the experiment at 850° C the curve for the vanadium alloys would have been the same at the high-vanadium end, but the horizontal line for the other alloys would have been higher up the ordinate. Thus, it can be deduced from the results of Harris *et al.* that the rate of scaling depends on the amount of V_2O_5 present on the surface of an alloy, irrespective of its source; this agrees with the results of other investigators and is emphasized in the first paper.

Harris and his co-authors have concluded that alloys containing less than 30% of iron will be reasonably resistant to vanadium pentoxide attack, as long as the vanadium content does not exceed 2%. This seems unwarranted by the evidence the authors have presented in Figs. 5 and 6. I would suggest that the vanadium content is revelant only to scaling in air, that alloys containing less than a specified amount of vanadium will not suffer excessive vanadium pentoxide attack in the absence of external contamination. If contamination occurs but is substantially less than the amount of vanadium pentoxide present in the scale due to the oxidation of the alloy, it will not affect the rate of scaling ; if it exceeds that amount it will have the same effect as on a vanadium-free alloy. Vanadium pentoxide attack depends on the amount of vanadium pentoxide in the scale, from whatever sources, and the vanadium content of the alloy is irrelevant.

The work of Professor Preece and his collaborators on the mechanism of vanadium pentoxide attack on chromium is very important. It is gratifying to have the guess confirmed that vanadium pentoxide fluxes away the protective layer of chromic oxide. The authors conclude correctly that the scaling resistance of the alloys will depend on the stability of the base composition of the alloy without chromium. In this context one might add that, in service, the alloys are exposed to atmospheres containing corrosive gases, and resistance to oxidation alone is not a sure guide.

The authors have reached the gloomy conclusion that the use of chromium as an element conferring oxidation resistance is ineffective in the presence of vanadium pentoxide, and that heat-resisting alloys which depend on the oxidation resistance normally conferred by chromium cannot withstand vanadium pentoxide attack. This view seems essentially correct and suggests that it is necessary, in the absence of an oil additive which suppresses vanadium pentoxide attack, to develop alloys whose resistance to scaling depends on elements other than chromium. Perhaps the most encouraging statement in the paper is that the attack on the silica sheets used to support the cones was surprisingly light. One thinks of the possibility of protective scales based on silica. Clearly, the use of high silicon contents in heat-resistant alloys is not a practicable proposition because it would be difficult to work the alloys, but it may be worthwhile in this context to draw attention to the claims of Fitzer and Schwab,[2] that steels with a silicon-rich surface layer produced by diffusion from a gas phase are highly resistant to vanadium pentoxide attack.

Dr. **N. Stephenson** (National Gas Turbine Establishment) : During corrosion work at the N.G.T.E. we found three distinct types of attack by fuel ash. The first was a uniform attack caused by vanadium pentoxide alone. We hope to publish soon an explanation of the reaction mechanism similar to that suggested by Professor Preece. The second form of attack was a very rapid uniform corrosion caused by ashes of low moderate sodium/vanadium ratio, particularly those corresponding in composition to the sodium vanadyl vanadates. Lastly, we experienced a localized catastrophic attack caused at high temperatures by ashes of high sodium/vanadium ratio. We do not understand fully the reaction kinetics for the last two types of attack.

For attack by vanadium pentoxide alone, the heat-resisting steels were not much inferior to nickel–chromium alloys, provided that the temperature did not exceed 700° C. However, contact with sodium vanadyl vanadates caused extremely rapid corrosion of the steels in air atmospheres, although nickel–chromium was affected only slightly. If specimens coated with sodium vanadyl vanadates were exposed to sulphur-bearing atmospheres simulating those encountered in a gas turbine burning residual fuel, the attack on the nickel–chromium material was that caused by the vanadium pentoxide content of the coating alone. On the other hand, the corrosion of the iron-based alloys, although diminished, was still very rapid. This beneficial effect of sulphur-bearing atmosphere is perhaps unique in the field of high-temperature corrosion, and we attribute it to sulphation of at least part of the sodium present in the ash. The laboratory data for Ni–Cr were supported by results from combustion rig tests, where corrosion rate was dependent only on specimen temperature and V_2O_5 deposition rate and was independent of fuel-ash composition.

The third type of corrosion, the localized catastrophic attack caused by ashes of high sodium/vanadium ratio, appears to be most detrimental to the nickel–chromium alloys : that is, to the material considered most resistant to straightforward vanadium pentoxide attack.

It seems important that in comparing and assessing the resistance of materials to attack by fuel ash by means of laboratory tests, generalizations should not be made about their effectiveness on the basis of V_2O_5 attack alone.

I notice that in their conclusions Harris, Child, and Kerr make the statement that the scaling indices determined from 70 to 1120 h enable the useful service temperatures in air to be assessed. I do not know whether they intended that to apply only to the ash-free condition or to attack in the presence of fuel ash. Even where attack is effectively that of vanadium pentoxide alone, the rate of corrosion is a function of ash-deposition rate as well as of temperature. Hence it is doubtful whether the scaling index could be relied on by the designer.

Another point of criticism is that in their cyclic tests one might tend to lose scale during temperature cycling, and this scale would contain vanadium pentoxide which is effectively lost from the test.

From a standpoint rather different from that put forward by Professor Preece, our studies of the reaction mechanism of vanadium-accelerated attack have led us to the

same conclusion. We think it is unlikely that a completely resistant alloy will be developed from a material depending on chromic oxide-rich film formation for its resistance to corrosion, and suggest that a completely resistant alloy will require its protective oxide film to be completely insoluble in and non-reactive with ash constituents. We agree that ash constituents, if soluble in the film, must not promote ionic transfer of the base-metal across the protective layer. However, unlike Professor Preece, we think that under the practical condition of continuous ash deposition the growth of a continuous, adherent, vanadium-contaminated layer might be tolerated as long as the layer does not interact with the fuel ash or the base-metal vanadates. Thus although accelerated oxidation might be rapid initially, a protective layer would be built up and the rate of attack would diminish in time.

In our earlier work we had noticed that a nickel–2% beryllium alloy was more resistant to attack by vanadium pentoxide than nickel–chromium alloys. The results of Professor Preece's preliminary survey of the beryllium-oxide–vanadium-pentoxide system, which were made available to us at an early date encouraged us to resume work with nickel-based alloys containing beryllium.

In the laboratory tests with natural and synthetic ashes we found that a nickel–4% beryllium material was 40–50% more resistant than a nickel–chromium-based, creep-resistant alloy. Additions of 4% of beryllium to a nickel–chromium alloy increased its resistance to attack by 30–50%. These beryllium-containing alloys are not subject to very rapid attack by the sodium vanadyl vanadates. Moreover, they seem much more resistant than nickel–chromium-based materials to localized catastrophic corrosion in contact with sodium-rich ashes.

If vanadium pentoxide is coated on to the nickel–4% beryllium alloys in the manner described by Harris, Child, and Kerr, on heating, the corrosive agent does not spread uniformly over the surface as with other heat-resisting alloys. Patches of clean metal remain, suggesting that the oxide film is partially protective. If vanadium pentoxide additions are made intermittently, there is an indication from tests still proceeding that the rate of attack diminishes in time. This contrasts with the behaviour of a nickel–chromium material under similar conditions where the rate of attack is constant.

We have attempted to promote the formation of a more protective beryllia film by pre-oxidation in the absence of fuel ash at low partial oxygen pressures. So far, these measures have caused no further improvement.

We feel that the preliminary results we have obtained from these materials containing beryllium are in a sense encouraging. Perhaps it is a far cry from preliminary laboratory tests on simple alloys to the long-life engine application of proven stress-bearing materials, particularly as beryllium is both toxic and expensive. Techniques for handling toxic materials have improved greatly in recent years, and the expense might be partly overcome by surface treatment of known alloys with beryllium.

For some time, it has been said that metallurgy can make no worthwhile contribution to a solution of the corrosion problem, but we hope that even the results of these few observations on nickel alloys containing beryllium will discourage such opinions.

Mr. **H. C. Child** (William Jessop and Sons, Ltd.): In the first paper, it is stated that chromium cannot be used as an addition to prevent attack by V_2O_5. Whilst I would agree that there is no known alloy containing Cr with entirely satisfactory resistance to V_2O_5, the generalization about the ineffectiveness of Cr is misleading.

It has been shown by several workers that nickel–chromium and cobalt–chromium platings are much more resistant to V_2O_5 than the simple nickel or cobalt platings. We and others have also shown that chromium imparts added resistance to V_2O_5 attack to many Ni, Co, and Fe base alloys. In view of this, chromium should be regarded as a most useful element in this field.

I realise that this difference in opinion arises largely from a different interpretation of the evidence available but I would welcome Dr. Lucas's comments.

Dr. **G. Lucas** (Brown Firth Research Laboratories): The addition of more chromium would also improve the scaling resistance in air, of course, so Mr. Child's comparison is no better. All he is saying really is that when you add V_2O_5 general corrosion conditions are far worse.

We look on this as comparative, as we point out in our paper. I think it is wrong to say that nickel-based alloys are more resistant to V_2O_5 attack. It is true that if you do a scaling test you have a lower result, but the nickel-based alloys are also more resistant to scaling in air. The relative behaviour of the alloys is still the same.

Mr. **F. H. Davis** (Metropolitan-Vickers Electrical Co., Ltd.): I should like to approach the problem from the engineering aspect since, as an engineering firm, we are interested in the operation of gas turbines on heavy fuel. Both papers are interesting and valuable, but they suffer from lack of correlation with practical experience.

Harris, Child, and Kerr have measured the resistance to attack in terms of scaling factor. This factor alone is not enough to judge the relative merits of materials for use in a turbine. No account is taken of the mode of attack and no microscopic studies are reported. The importance of this aspect is illustrated by our experience of a turbine running on heavy fuel. In this particular engine the nominal inlet temperature was 650° C, but for various reasons local 'hot spots' occurred in which the temperature was of the order of 800° C. At this temperature the nickel-based alloy blades suffered local intercrystalline attack penetrating to a depth of about 0·005 in. Such attack could materially affect the fatigue strength long before enough general scaling to lose the blade form had occurred. On the other hand, a simple 18/8 austenitic steel, whilst scaling generally at a faster rate, would not suffer the local penetrating attack.

I also suggest that the amounts of V_2O_5 added have been too small in relation to quantities obtained in practice. If I read it correctly, the authors have added 1·5 mg/cm² in 70 h. For a typical engine in which the blading comprises 60% of the wetted area, and assuming an ash content of 0·04% with 75% active constituents, the potential rate of delivery of ash is 3 mg/cm²/h. Even assuming a fall-out of only 10%, the rate of ash deposition is 15 times that used in the author's tests. I feel that in an investigation of this nature which cannot be fundamental because of the complexity of the problem, but is attempting to simulate engine conditions, ash quantities nearer those in practice should be used.

This is emphasized because we differ from the authors about the effects of titanium and molybdenum in the nickel- and cobalt-based alloys. In our experience the presence of these constituents increased the rate of attack. In addition, in the case of the titanium-containing nickel-based alloys, we consider that the intercrystalline nature of the attack resulted from the presence of titanium carbide as a precipitated phase at the grain boundaries. This belief is supported by the extremely poor resistance of titanium carbide bonded with a cobalt matrix. I suggest very tentatively that this difference in result may be accounted for by the small quantity of V_2O_5 used by the authors. It is conceivable that, with the small quantity of V_2O_5 used, a high-melting-point vanadate was formed with the Ni and Cr oxides. In this condition oxidation will proceed only by solid diffusion. With larger quantities, the V_2O_5 remains molten and attack probably proceeds by simple solution.

I wish to endorse Mr. Shirley's comments. We, too, have found in laboratory tests that iron–chromium alloys, with Cr contents of up to 40% without nickel, are more resistant to attack than the nickel-based alloys. Under these circumstances, it seems unreasonable to blame the breakdown of scale resistance on chromium oxide.

Our main efforts have been in the direction of protective coatings rather than improving alloys. In order to do this, like Lucas, Weddle, and Preece, we have attempted to formulate rough V_2O_5–metal-oxide systems. We were not quite so pessimistic as they were about using the cooling-curve method and employed platinum crucibles and thermocouple sheaths. The attack on the platinum in the total time proved negligible. Our curves agree substantially with theirs, but in the V_2O_5–Al_2O_3, V_2O_5–Cr_2O_3, and V_2O_5–BeO systems, our liquidus was a straighter line up to about 20% metal oxide. From the shape of the liquidus and X-ray patterns on the mixtures, it is clear that these three oxides are not soluble in V_2O_5. The irregular line obtained in the first paper may have been due to the relative insensitivity of the Segar cone technique.

These three oxides which are refractory and insoluble in molten V_2O_5 should offer the best resistance to attack. Nickel oxide, on the other hand, forms a eutectiferous series with V_2O_5 and suffers in the plated form from severe intercolumnar attack.

From tests, beryllium offers the most promise. I suggest that this could best be used as a diffused coating. The solid solubility of beryllium in iron is about 8%, that in nickel 3%. The main advantages of using the beryllium as a diffused coating rather than as an alloying addition are that sufficient beryllium could be introduced to improve the oxidation resistance without impairing the mechanical properties. In addition, the price should be materially less. The disadvantage with a diffused coating is that local breakdown could lead to catastrophic attack, so that attention must be paid to ensuring complete protection. Such a breakdown could occur from spalling, and this would preclude the use of Si-diffused coatings which would otherwise also be potential coatings.

AUTHORS' REPLIES

Professor **G. Preece** and his co-workers : Our statement that a chromic oxide film is not resistant to vanadium pentoxide has been challenged, but our experiments show conclusively that, while it may be attacked more slowly than other oxide films, it is not wholly resistant to attack.

Oxidation experiments at higher temperatures, suggested by Mr. Burns, are complicated by the fact that, above its melting point, vanadium pentoxide decomposes to V_2O_4 and soluble oxygen. The higher the temperature the more the composition differs from V_2O_5. The "curious irregularities in the weight during the cycle" could be related to this decomposition. We feel that our Segar cone technique minimized interference with the melting-point determinations by this decomposition and that the cooling-curve measurements mentioned by Mr. Davis could be subject to errors from this source.

We were pleased to learn that Dr. Stephenson's conclusions agree with ours. It is very doubtful, however, that under practical conditions of continuous ash deposition a protective layer could be built up. If the ash contains V_2O_5 the inner layers of ash will continue to react with the surface film on the metal irrespective of the subsequent ash-deposition rate.

Mr. **G. T. Harris** and his co-workers: In reply to Mr. Shirley's query about Mo-bearing steels, we know of no case where such steels have given trouble due to catastrophic scaling under gas-turbine conditions and we agree that the molybdenum effect is at present of little practical importance.

Several speakers pointed out the danger of using laboratory data for design purposes and found fault with the experimental conditions as not representing those existing in practical applications. While we are in complete sympathy, we feel that laboratory data obtained under as simple conditions as possible and on as wide a range of materials as is practical are the best contribution the metallurgist can make to the problem of scale resistance in general and V_2O_5 attack in particular. The engineer must interpret these data in light of his own conditions; e.g. to answer Dr. Stephenson about useful service temperatures, our V_2O_5 scaling indices obviously only hold for our rate of application of V_2O_5. The engineer should, however, be able to assess the potentialities of a material from limited laboratory tests even if they are not directly applicable to his own conditions.

Mr. Sachs and Dr. Stephenson have suggested respectively that alloying additions of silicon and beryllium might present a metallurgical solution to the residual fuel-ash problem. This is worth following up but it is unlikely that any resultant alloy would also be forgeable, and if this were not regarded as a necessity, then that the alloy would have the necessary creep strength, ductility, etc. The high-temperature strength field has been studied exhaustively and few alloy systems have escaped attention. The promising alloys exploited commercially are all prone to V_2O_5 attack. To combine the attributes of high-temperature strength and resistance to V_2O_5 may well be impossible. Diffusion coatings of silicon or beryllium might be a solution, but the practical problems of coatings show the difficulties involved—grain growth, distortion of finished parts, 'Achilles heels' where diffusion coatings are thin or non-existent, and mechanical damage in service.

It is of no consequence whether V_2O_5 in the scale comes from external or internal sources, and our value of 2% V in the alloys containing less than 30% Fe is a limit based on the experience that for the base in question this percentage of vanadium does not result in sufficient V_2O_5 in the scale to cause catastrophic scaling.

Mr. Davis's findings that Mo and Ti have a deleterious effect on V_2O_5 attack are of interest. It is well known that Mo has such an effect on many base compositions, and the interesting point of our results is that many tried Ni–Co–Cr–Fe bases are not so affected. That Ti should also play such a part is so far as we know new evidence, and further details of the alloy base used and the titanium contents would be welcomed.

We feel sufficiently strongly about the need for observing anomalies in scaling rate that we are converting all our equipment to permit continuous weighing of the specimen, as the present technique does not show up the effects Mr. Burns describes.

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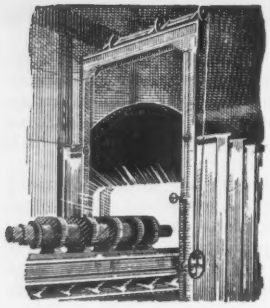

IRON AND STEELWORKS ENGINEERING

The Iron and Steel Engineers Group

Modern Forging Presses and Their Control

By R. M. L. Elkan and J. T. Lewis

SYNOPSIS

The paper briefly examines the factors arising in the choice of the method of drive for modern high-speed forging presses. Certain aspects of hydraulic circuit design are examined, and also structural methods for the determination of the stresses set up throughout the press structure due to eccentrically and directly applied loads. A modern forging press is basically defined as a unit capable of exerting 85–90% of its rated tonnage simultaneously giving high penetration speeds in the region of 6–10in./s. The press must also be capable of withstanding its rated tonnage at an eccentricity the magnitude of which depends on press size and nature of work. 1259

IN RECENT YEARS great progress has been made in the endeavour to obtain a more efficient utilization of forging presses and plant, resulting in increased annual outputs. An appreciable amount of literature has been published, reviewing the change in construction of presses and the alternatives in the choice of drive and enlarging on the need for a more critical control over the efficient working of existing plants.

Sanderson and Frith[1] in a survey of heavy forging presses in the world gave a good summary of the many features of press design and their trends. A historical survey was made by Stone,[2] in which he also indicated the problems resulting from eccentric loading, and how different designs overcome this with varying degrees of effectiveness. Alvey[3] described the performance of six directly driven forging presses in one plant, all operated by electrically driven slow-speed triple-plunger water pumps. These papers are major contributions to the published literature of recent years, some others of which are given in the Bibliography.

There are a very great number of forging presses in all parts of the world, many of which are rapidly becoming obsolete; such presses are now 40, 50, and even more years old. As a result of British leadership in the earliest days, many very old presses are still found to be working in the U.K., testifying to the skill of the designers and the quality of the manufacture. However, in many installations the performance is economically so poor that much reorganization and modernization of presses and plants is now being carried out.

The present paper is limited to consideration of two main subjects—the need for a modern forging press to withstand eccentric loading, and an examination of some elementary principles of hydraulic circuit design; it is further restricted to what are known as 'free forging presses' as distinct from 'die forging presses.' Although such limitations narrow the scope of a paper, it was felt justified to examine these topics, and also to make a brief reassessment of the problem of 'hydraulic accumulator drive' or 'direct drive,' which confronts those engaged in the design and installation of forging plant.

As it is possible to touch only upon the fringes of a great field of study, much of the paper may not be new to many, but if it stimulates discussion it will have achieved its object.

CAPACITY AND PERFORMANCE OF MODERN PRESSES

A modern press can be defined as a unit robust enough in its construction to withstand the arduous and varied duty which may be imposed upon it up to its rated power, and at a loading which is nearly always eccentric to either the longitudinal or the lateral axis of symmetry. In the past only very moderate working speeds were available, but now much

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higher speeds are attainable, a press has to be fast working and yet capable of exerting something approaching its rated power (say 90% of its rating) at penetration speeds of 6–8 in./s. The ability to withstand eccentric loading is now also being increasingly demanded in specifications, and a closer study, although only exploratory and approximate, of its effect on the press structure, is a necessity.

By careful circuit design it is possible to combine the application of, say, 85–90% of the rated press power on an ingot, together with high penetration speeds. It was once accepted as inevitable that one-third or one-quarter of the press rating was lost in 'speed.' There was no real theoretical basis for this (a possible explanation is indicated in Appendix I) and it can only really be explained by insufficient data being available on pressure losses through valves, pipes, and other obstructions.

In addition to higher penetration rates, the idle movement speeds of the moving crosshead (i.e. 'advance' and 'return') have also been increased, and are now about 12–15 in./s.

These high penetration and idle speeds are required because it has been found in recent years that they enable much more work to be carried out per heat, thus reducing the number of reheats required in the furnace per forging, with consequent savings in labour, handling time, and fuel, better furnace utilization, etc. Instances are known of parts being completely forged in three or four heats, where previously six or seven were required.

Columns 1–8 of Table I give data for modern presses; these are given as being typical and should not be regarded as a rigid statement. For the ingot diameters after upsetting (column 4), the 'stalling' pressures (2–2·5 tons/in^2) are generally accepted and give an approximate indication. However, on the more recent fast-acting presses (penetration speeds 6–10 in./s) it has been found that once an upsetting stroke has begun and is carried out continuously, the operation can be carried on down to specific pressures as low as 1 ton/in^2, depending on ingot size and the nature of the steel. In such upsetting operations it is important that these high speeds are available as well as 85–90% of the rated press power; this results in better forgings and in savings in forging time.

Apart from not losing so much heat, as a result of shortening the time in the press, a certain amount of heat is also actually generated by the work being done on the forging at high rates of penetration.

The additional availability of power with speed has made it possible to use wider forging tools: i.e. more effective utilization of presses is now obtained, enabling larger ingots to be worked with any given press tonnage.

The data in columns 2, 3, and 4 must be regarded as very general only, for the use of less than normal-width tools enables ingots larger than the nominal size to be handled, although results may not be quite as satisfactory. Although for some special alloy steels there may be a limiting speed of penetration, to exceed which would cause a too rapid rate of working, high penetration speeds are desirable for most steels, particularly some high-alloy steels, as the temperature range within which they can be forged is relatively small, and the time available for the smaller-sized ingots is correspondingly less. Column 7 gives suggested duty specifications for modern presses.

To summarize, higher outputs are achieved from an up-to-date press by

(i) The use of higher penetration speeds, and also higher advance and return speeds
(ii) The utilization of a press at a tonnage of 85–90% of its rating together with high working speeds; this implies careful and correct circuit design
(iii) Careful valve design and by the use of modern servo actuating gear, giving instantaneous and finger-light control to the operator
(iv) By the increasing utilization of moving-table gear for tool changing.

Higher outputs are achieved from up-to-date plants by

(i) The fullest use of ingot manipulators where applicable
(ii) Employing more efficient and powerful electric cranes and handling gear. Enough cranes must be available so that the transport of ingots and finished forgings does not delay forging operations
(iii) The installation of furnaces of ample capacity to keep pace with and make possible the effective utilization of the greatly improved performance of the modern press
(iv) Careful planning of the layout, taking the requirements of the flow of material fully into account.

CHOICE OF PRESS DRIVE

Types of Drive Available

The history of the types of drive used for forging presses shows that seven methods have been used:

(1) Steam-driven pumps
(2) Steam-hydraulic intensifier
(3) Air-hydraulic intensifier
(4) Direct pump drive
(5) Weight-loaded accumulator and water pumps
(6) Air-hydraulic accumulator and water pumps
(7) Air-hydraulic accumulator, water pumps, and hydraulic intensifier.

The following remarks summarize the present position:

(1) Steam-driven pumps are no longer used.

(2) The steam-hydraulic intensifier drive, though still in use in a fairly large number of forges, is rapidly becoming obsolete, for the cost of power used per ton of forging is no longer competitive when taking all factors into account; also steam is now not so readily available in works. Such factors as the need to raise steam long before forging takes place and the problem of stand-by losses associated with all steam-generating plant have gradually caused the displacement of steam-driven apparatus by that driven by electric power. There was also an absence of sensitive control when 'cutting through' and the moving crosshead could not be worked at a controlled speed.

(3) The efficiency of air-hydraulic intensifiers was also not sufficiently economical in operation.

(4) Direct pump drive found more application in the U.K. than abroad. It may be used equally well with single-, double-, or triple-cylinder presses, for there is no need to provide pressure stages by cylinder selection. Direct drive is generally by means of electrically driven 3-throw reciprocating water pumps, running at 80–220 rev/min, depending on the size of

Table I

DATA ON MODERN FORGING PRESSES

1	2	3	4	5	6	7	8
Size of Press, tons	Nominal Ingots Handled, tons	Nominal Mean Dia. of Ingot, in.	Largest Ingot Dia. after Upsetting,[a] in.	Press		Suggested Duty Specification[b]	UP-SETTING[c] Maximum Stroke Required Continuously at Full Power, in.
				Daylight, in.	Stroke, in.		
500	2	18	16	70	32	25 strokes of 3 in./min 45–55 " " 1½ " 80–100 planishing strokes per min (½ in. nom.)	25
800	5	24	20	78	40	20 3½ 40–50 1½ 80–100 (½ in. nom.)	30
1000	10	29	23	88	44	18 4 40–50 1½ 80–100 (½ in. nom.)	35
1500	20	42	28	112	55	15 5 30–40 2 80–100 (½ in. nom.)	45
2000	30	50	32	130	68	12 6 30–40 2 80–100 (½ in. nom.)	50
2500	45	58	36	140	70	10 6½ 30–40 2 80–100 (½ in. nom.)	55
3000	60	62	39	150	75	10 7 30–40 2 80–100 (½ in. nom.)	60
4000	80	72	50	168	85	9 7½ 30–35 2 60–80 (½ in. nom.)	65
5000	100	80	57	190	100	8 8 30–35 2 50–60 (½ in. nom.)	70
6000	125	85	62	225	115	7 9 25–35 2 50–60 (½ in. nom.)	75
8000	170	95	72	245	120	6 10 20–30 2 50–60 (½ in. nom.)	80
10000	220	100	80	258	130	5 11 20–30 2 40–50 (¾ in. nom.)	85
12000	260	110	87	265	135	5 11 20–30 2 40–50 (¾ in. nom.)	90
15000	300	115	98	280	135	4 12 15–25 2 40–50 (¾ in. nom.)	95

Notes and Assumptions Made in Table I

a. Press stalls at a specific ingot pressure of approx. 2·5 tons/in² up to 3000 tons, 2 tons/in² over 3000 tons. (In recently installed presses, owing to the availability of high speed at nearly the full press rating, these figures were greatly improved upon.)
b. The number of planishing strokes as laid down for the larger presses was not limited by hydraulic considerations. It was considered adequate, bearing in mind the handling problem with large ingots. From a purely hydraulic aspect these figures could be doubled.
c. 6–10 in./s upsetting speeds.
d. 6–10 in./s penetration speed. Allowance made for compressibility of water.
e. Return power taken as 10% of main press power for purpose of calculations. Return stroke taken as 150% of main pressure stroke. Allowance made for fluid compressibility.

Table I (*Continued*)

DATA ON MODERN FORGING PRESSES

	9	10	11	12	13	14	15	16	17	18	19	20	21	22	23	24
Size of Press, tons	Accumulator Drive[d]		Direct Drive[e]				kW Installed					kWh Consumption per av. Operating Hour				
	Pump Output Required, gal/min	Accumulator 'Useful' Capacity, gal	Pump Output (gal/min) Required at Penetration Speed of:				Accumulator Drive[f]	Direct Drive,[g] at				Accumulator Drive[h]	Direct Drive,[i] at Penetration Speed of:			
			2 in./s	4 in./s	6 in./s	8 in./s		2 in./s	4 in./s	6 in./s	8 in./s		2 in./s	4 in./s	6 in./s	8 in./s
500	92	25	133	247	361	475	322	210	390	570	750	92	48	67	83	102
800	139	40	216	398	580	762	487	342	630	916	1205	139	78	108	133	164
1000	181	60	276	504	732	960	634	437	795	1160	1520	180	99	138	168	207
1500	280	110	410	752	1094	1436	980	650	1188	1730	2270	279	147	204	252	309
2000	368	160	564	1020	1476	*1932*	1290	891	1610	2330	*3050*	368	202	277	338	*415*
2500	453	225	712	1282	*1852*	*2422*	1590	1128	2030	*2930*	*3830*	453	240	349	*425*	*522*
3000	549	300	868	1552	*2236*	*2920*	1920	1370	2460	*3530*	*4610*	547	285	422	*512*	*627*
4000	728	425	1192	*2104*	*3016*	*3928*	2550	1890	*3330*	*4770*	*6200*	725	394	*573*	*690*	*843*
5000	900	550	1544	*2684*	*3824*	*4964*	3150	2440	*4250*	*6050*	*7850*	897	508	*732*	*877*	*1070*
6000	1059	725	*1836*	*3204*	*4572*	*5940*	3700	*2900*	*5080*	*7220*	*9400*	1050	*605*	*872*	*1045*	*1280*
8000	1306	1050	*2340*	*4164*	*5988*	*7812*	4570	*3700*	*6570*	*9450*	*12350*	1300	*770*	*1130*	*1370*	*1680*
10000	1550	1450	*2980*	*5260*	*7540*	*9820*	5430	*4700*	*8310*	*11900*	*15500*	1550	*893*	*1205*	*1515*	*1970*
12000	1885	1800	*3616*	*6352*	*9088*	*11824*	6600	*5700*	*10050*	*14350*	*18700*	1880	*1085*	*1455*	*1840*	*2375*
15000	1982	2450	*4244*	*7664*	*11084*	*14504*	6940	*6700*	*12100*	*17500*	*23000*	1975	*1272*	*1755*	*2220*	*2920*

f. Volumetric efficiency 93%, mechanical efficiency 81% (i.e. gear box and hydraulic packing, and mechanical resistance in pump).

g. To present the 'direct-drive' case in the most advantageous way, it has been assumed that a flywheel system is fitted to the high-speed side of gearboxes of water pumps and oil pumps, where applicable. Installed motor h.p. taken as 60% of hydraulic h.p., motor efficiency as 85%.

h. Accumulator pumps on load 20% of operating hours, idling for remaining 80%. Power consumption when idling 10% of full load.

i. Percentage of operating hours pumps are on-load:

Penetration Speed	2 in./s	4 in./s	6 in./s	8 in./s
Up to 2000-ton press	14%	8%	5%	4%
2500–8000-ton press	12%	8%	5%	4%
10,000–15,000-ton press	10%	5%	4%	3%

As an approximation and a mean figure when on-load under the above conditions, kWh consumption was based on kW installed. Power consumption when idling 10% of on-load kW.

pump, the higher speeds being generally associated with pumps of smaller output. The largest single pump unit in the U.K., installed with five other forging presses working with the same drive, has an output of 1432 gal/min at $2\frac{1}{2}$ tons/in^2. Recently some smaller forging presses have been operated by direct-oil pump drive in the U.S.A., and this system has also been used for some low-tonnage presses recently built in the U.K., using high-speed multiple-plunger pumps, directly driven by motors at 1440 rev/min.

(5) The use of weight-loaded accumulators with water pumps was tried early, but the high kinetic energy of the moving parts, with resulting water hammer, made quick forging impossible and soon caused this drive to be dropped.

(6) Air-hydraulic accumulator drive has found very wide application, as it is extremely flexible and useful for forging presses. When used in conjunction with a 3-cylinder press it provides three power stages at which the press can be worked.

(7) Air-hydraulic accumulator drive with intensifiers is also used on modern plants, though less frequently than (6). However, for large and special-purpose presses (i.e. those used for everyday forging work but required for the occasional upsetting of very large ingots) this arrangement can be economically attractive. Also, the use of intensified higher pressures keeps the upper platens and general structure of such presses narrow. If this method is adopted for a three-cylinder press, six pressure stages are available.

Advantages of Direct Drive

Where this type of drive is applicable, having regard to the quantity of fluid required per unit of time, the method is economical, for the power absorbed is proportional to the resistance being met.

Within certain limitations, discussed later, it is possible on some forms of this drive and for some applications, to utilize flywheels and to overload motors for short periods (i.e. the adoption of a form of ' accumulator,' in this case a mechanical flywheel), thereby keeping down their sizes.

The slow-speed triple-plunger pump has been proved to be very reliable over many years of service and thus its liability to breakdown is very small. Some of the high-speed pump units are not so reliable as the slower type.

For the smaller presses when using high-speed multiple plunger pumps, no gearing is required between the pumps and motors.

Disadvantages of Direct Drive

High penetration speeds require large pump output, which puts a definite limit to the application of this drive above a certain press size and speed.

The utilization of presses is not always satisfactory as concessions often have to be made in the maximum working speed, to keep the number of pumps down to a reasonable figure; such limiting speeds in the past were about 2 in./s.

Considerable momentary electrical loads are liable to be put on any works main to meet peak demands.

The need to add ' accumulators ' in the form of flywheels slightly offsets some of the advantages for this drive.

Speed selection by pumps complicates the circuit.

Advantages of Air-Hydraulic Accumulator Drive

High speeds of forging available (i.e. any desired water volume per second) can be obtained by appropriate circuit design.

When upsetting, it is possible to apply continuous pressure strokes at 8–10 in./s.

The pump output is settled on the mean demand over the period of working, not on any peak requirement; this results in optimum utilization of the press, as no concessions have to be made in available penetration speeds.

Stepless speed variation can be obtained with finger-light control when using servo-operated valve equipment. Circuits are simplified, and more than one press can be run off one centralized station.

Disadvantages of Air-Hydraulic Accumulator Drive

Pumps have to work against a virtually constant head, irrespective of resistance; this is largely overcome by using multiple pressure stages in presses to make economic utilization possible.

When intensifiers are used there is a limit in the pressure stroke that can be obtained without stopping, but this is overcome by the use of modern multiple intensifiers, which then function in effect as pumps.

If no adequate pressure stages are provided, and the press is worked at a power considerably below its rating, speed control implies throttling of the main inlet valve, with a consequent heat generation in the system: i.e. speed control wastes power.

[*Note:* The need for speed control also exists on direct drive, where it is possible to bring pump units in and out of action. However, switching motors on and off is not a satisfactory expedient while actually forging, and the pilot-operated unloading of pumps implies considerable complication in valve gear. Thus when speed regulation by throttling is carried out, the same objection, that of heat generation, would apply with direct drive also.]

WORKING FLUID

When water is used, as in most forging-press installations, 2–3% of soluble oil is added to avoid corrosion effects in the system and to act as a lubricant. All modern systems are ' closed,' i.e. the same water is used continuously, only very small amounts being added occasionally for ' making-up.'

Water is cheap and non-inflammable. As large volumes can easily be used, better cooling is effected than with oil, heat transfer between tanks, pipes, etc., and the surrounding atmosphere is more favourable, and its specific heat value is much higher than that of oil, which means that the tendency for the temperature of water systems to rise is smaller. Slow-speed water pumps are very robust and are not liable to breakdown.

Oil has advantages on the smaller slow-working presses. Corrosion is less than with water. The control valves are often more compact than water valves, but they are delicate mechanisms and, as not all forges are free from dust, these valves (some of which are of the packingless piston type with lap-fits in the bores) may under some conditions be more liable to damage than equivalent water valves. With oil there is the risk of picking up dust particles on

rams and exposed reciprocating parts. The oil of press circuits has to be kept scrupulously clean, and leakage in a forge is dangerous to personnel who may slip; apart from being dirty, it is detrimental to the foundations.

Oil is certainly inflammable, but the danger can be lessened by providing guards on exposed piping; a more serious danger might be caused by sudden leakage from a ram packing. Thus the flash point of these oils must be kept as high as possible; non-inflammable oils would be invaluable if they could be satisfactorily developed. Existing non-inflammable fluids are marketed at a prohibitive cost, and their very high specific gravity makes their use in high-speed pumps impracticable.

There has been much discussion of the viscosity properties of oils used in some press circuits, largely because different oil-pump manufacturers have, of necessity, to use oils of different viscosities in their pumps.

High-speed multiple-plunger pumps use oil of low viscosity (approx. 120–160 Redwood I seconds at 100° F) with 'flat' pressure and temperature *v.* viscosity curves. Although the use of low-viscosity oils implies closer clearances and manufacturing tolerances (sometimes down to 1 μ/in. dia.), with a consequent greater risk of 'dirt-lock' and abrasion of the finely lapped bores through the inevitable inclusion of small particles from a dirt-laden atmosphere, they do enable higher valve and pipe velocities to be used than would be possible with thick oils.

The above remarks are strictly confined to the application to forging presses. With other types of press, totally different conditions may exist, where some of the difficulties indicated may be unimportant.

USE OF FLYWHEELS WITH DIRECT DRIVE

Flywheels have in the past been fitted to the high-speed side of gear boxes driving the heavy triple-plunger water pumps; they run at 600–750 rev/min, their function being to keep down the motor size.

Flywheel systems running at 1500 rev/min and coupled directly to large high-speed multiple-plunger pump units have not, to the authors' knowledge, yet been utilized. Any such scheme would have to be examined carefully, for the rate at which energy can be taken from a flywheel is related to the torque-speed characteristics of the motors and to its own dimensions, which are limited by practical considerations.

Whereas there would probably be no insuperable difficulties as long as the length of the full-pressure stroke required by a press is short, its actual value being limited by the permissible speed drop of the flywheel, a continuous upsetting stroke which would necessitate energy being taken from a flywheel through a period of, say, 40 s would probably not be easily catered for.

A speed drop of 15% or even 20% of a flywheel would cause an equal diminution in pump output and press speed.

SELECTION OF WORKING PRESSURE

Air hydraulic accumulators have been used at working pressures between 200 and 300 atm. Generally 350 atm is not exceeded, as a marked amount of oxygen absorption into the water appears to take place at higher pressures. In the latest plants working pressures of 300 atm, a reasonable compromise of many factors, have generally been adopted.

The use of high working pressures reduces pipe and valve sizes to some extent, and also the amount of liquid that has to be moved for each stroke of the press.

It may in certain circumstances be advantageous on direct drive to work at 400 atm or even slightly higher, as used to be the practice in steam-hydraulic intensifier circuits. Since in some oil circuits pipe-line flow may be streamline and not turbulent, the advantage of keeping working pressures reasonably high, and therefore the volume flowing per unit of time correspondingly low, is apparent.

ECONOMICS OF VARIOUS METHODS OF DRIVE

Columns 9–24 of Table I show the pump output and accumulator capacities required for various conditions, all of which had to satisfy the duty specification indicated in column 7 (assumptions made are indicated at the bottom of the chart). The calculations in connection with the kW installation required and the kWh consumption are purely tentative and are only intended as a comparison, without any rigidity about assumptions made, either in principle or in detail. Conditions in plants vary greatly, and it has been found convenient to retain the definitions used in previous literature of a forging hour as *the time a forging is actually in the press*, and an operating hour as *the time the forging crew is actually available for forging operations*. The number of forging hours vary from plant to plant, between 40% and 60% of the operating hours.

For the direct-drive calculations, the installed motor h.p. has been taken as 60% of the hydraulic h.p. This, especially in the case of the slow-moving triple-plunger pumps, implies the use of a flywheel system. When endeavouring to assess the kWh consumption, assumptions have had to be made concerning the percentage of time at which pumps were on load under various conditions.

In Table I, the values in lighter type are included mainly for academic interest, since at the present state of development of pumps a direct-drive system greatly in excess of a total output of 1500–1600 gal/min is out of the question.

The largest high-pressure reciprocating pump unit ever made was a 1432-gal/min unit working at 2½ ton/in², and it is unlikely that larger units will be developed. Thus, 1600 gal/min would represent, say, eight 200-gal/min pump units, and by reasons of physical dimensions alone, quite apart from other factors, the limit for direct-drive installations will lie somewhere in this region.

Columns 20–24 of Table I show that, on the assumptions made, the accumulator system holds its own with the direct-drive system on power consumption, at speeds of 6 in./s and over. Saving in power consumption is claimed as the overall factor in favour of direct drive, most others weighing heavily in favour of the accumulator system, especially in view of the rates of penetration now required. On first installation costs, the oil drive can hold its own

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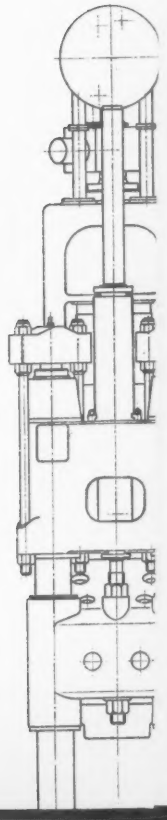

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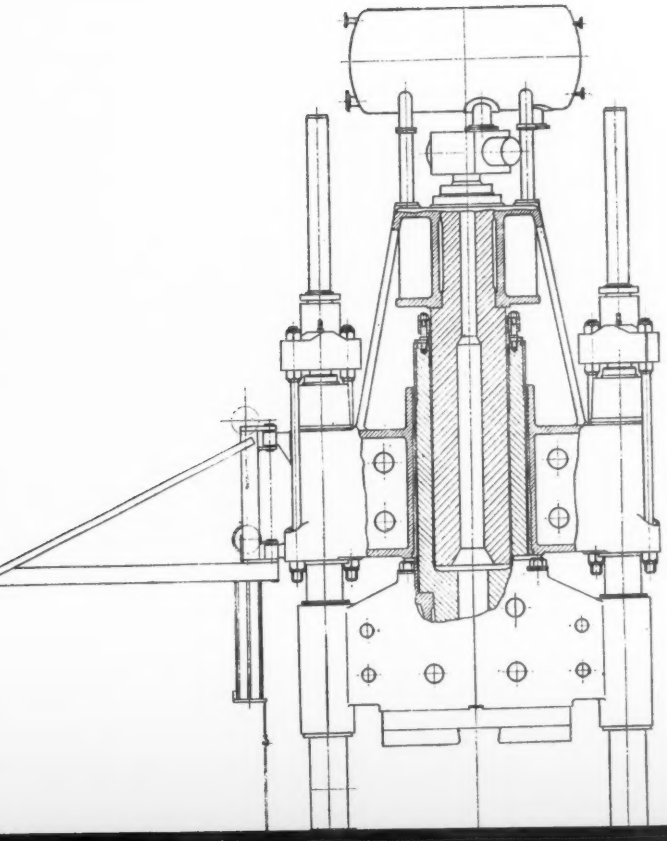

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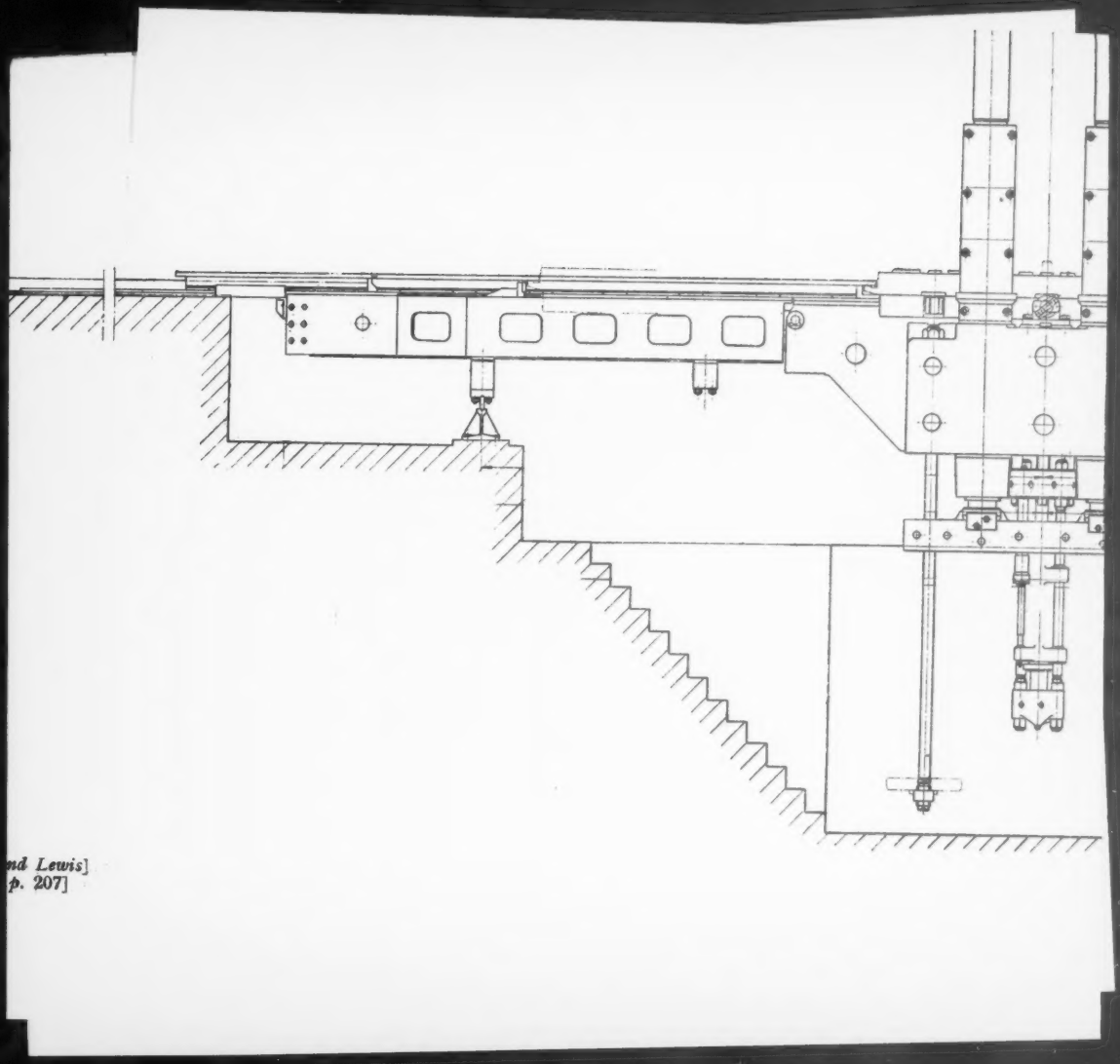

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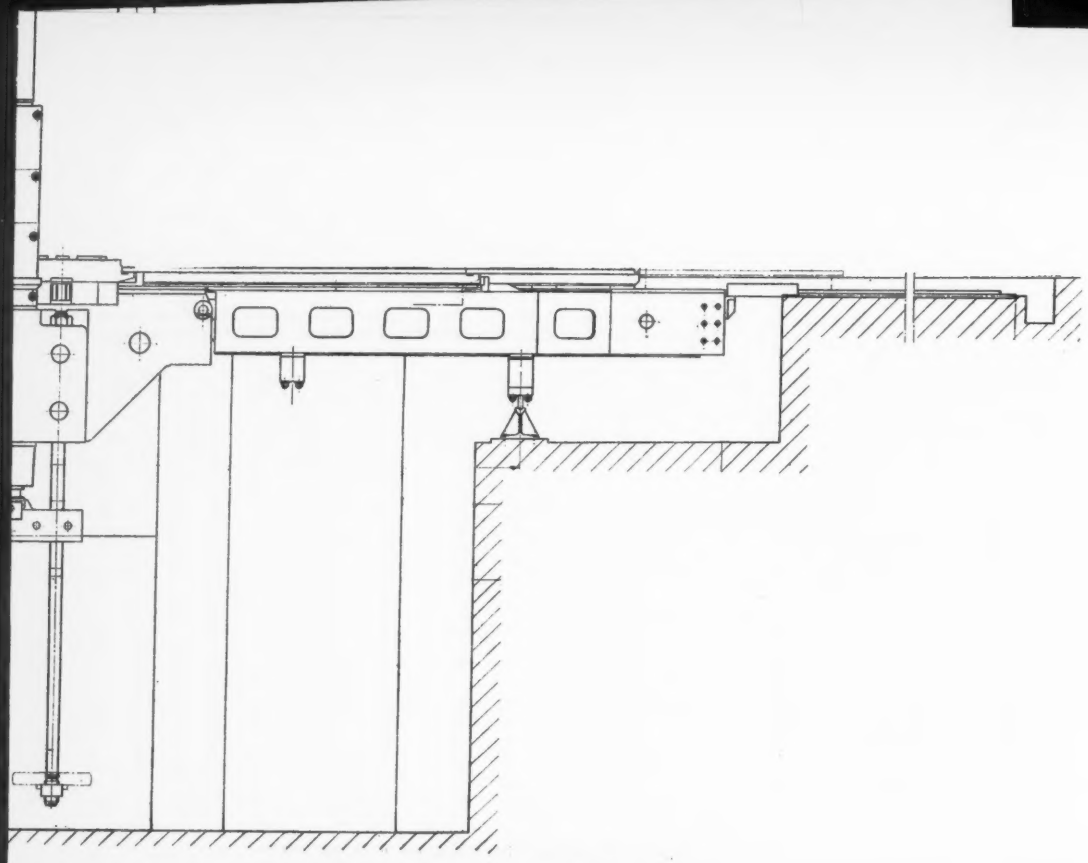

Fig. 3—Modern single-cylinder forging press

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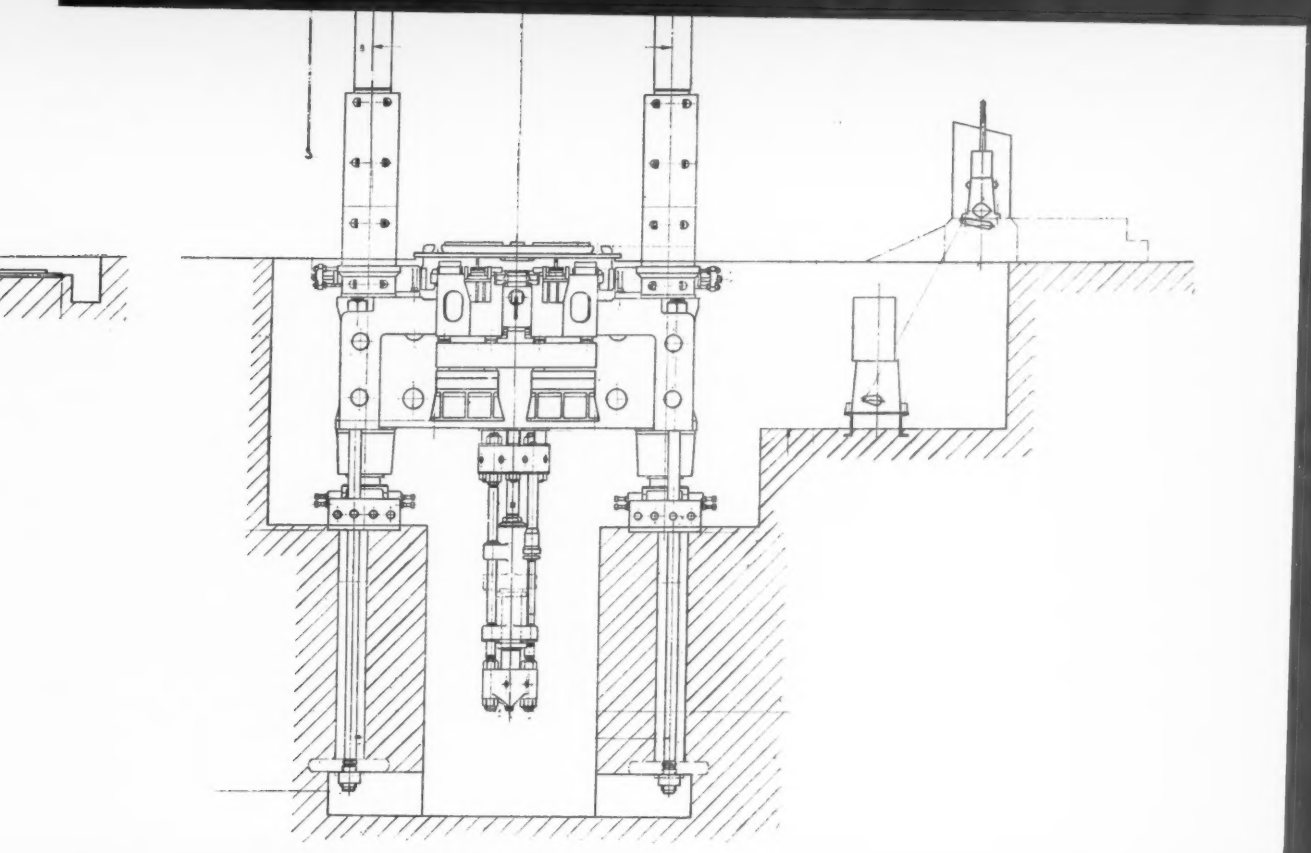

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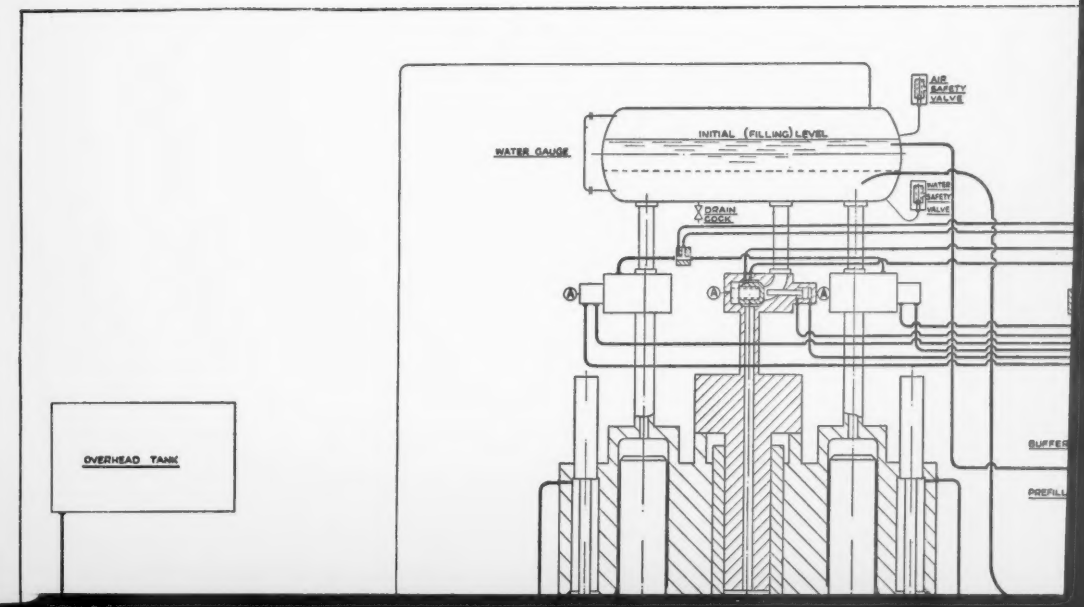
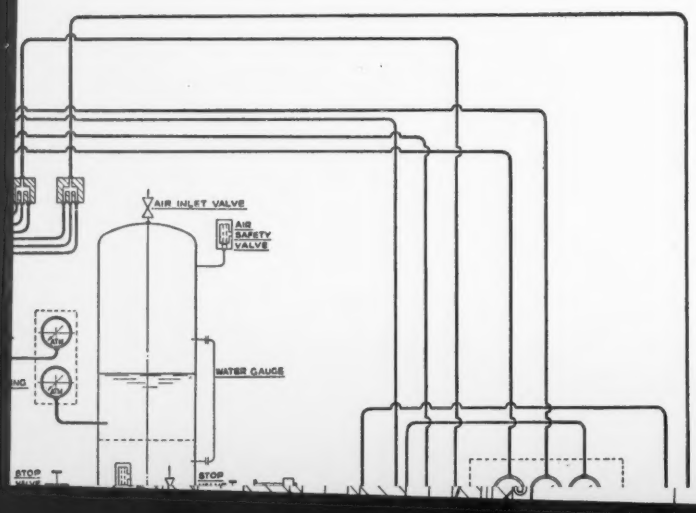
OVERHEAD
XUM


AIR SAFETY VALVE
INITIAL (FILLING) LEVEL
WATER GAUGE
WATER SAFETY VALVE
DRAIN COCK
OVERHEAD TANK

XUM



QUICK FORGING (MAIN LEVER ONLY)

NORMAL FORGING (LEVERS ARE COUPLED TOGETHER)

XUM

NOTE.
AIR VALVES FITTED AT HIGHEST POINTS OF ALL PIPES
ALTERNATIVE PIPING & CONNECTIONS PROVIDED FOR
THE THREE ALTERNATIVE LOCATIONS OF THE EJECTOR
& LOCKING CYLINDERS.

A	AIR RELEASE VALVE
D	DRAIN VALVE
N.R.V.	NON-RETURN VALVE
P.I.	PRESSURE INLET VALVE
P.O.	PRESSURE OUTLET VALVE

R ONLY)
D TOGETHER)
AUXY LEVER FIXED

D

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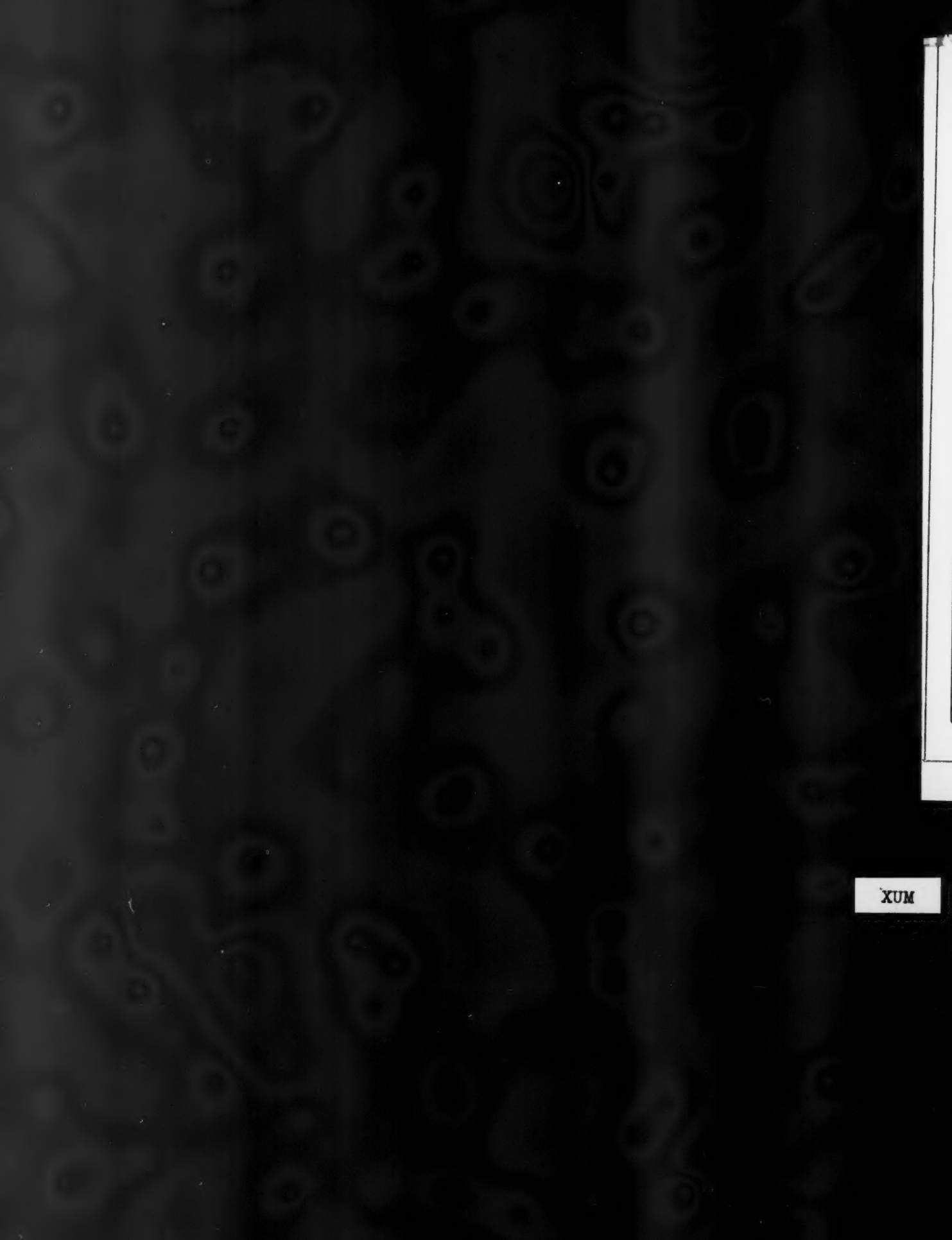
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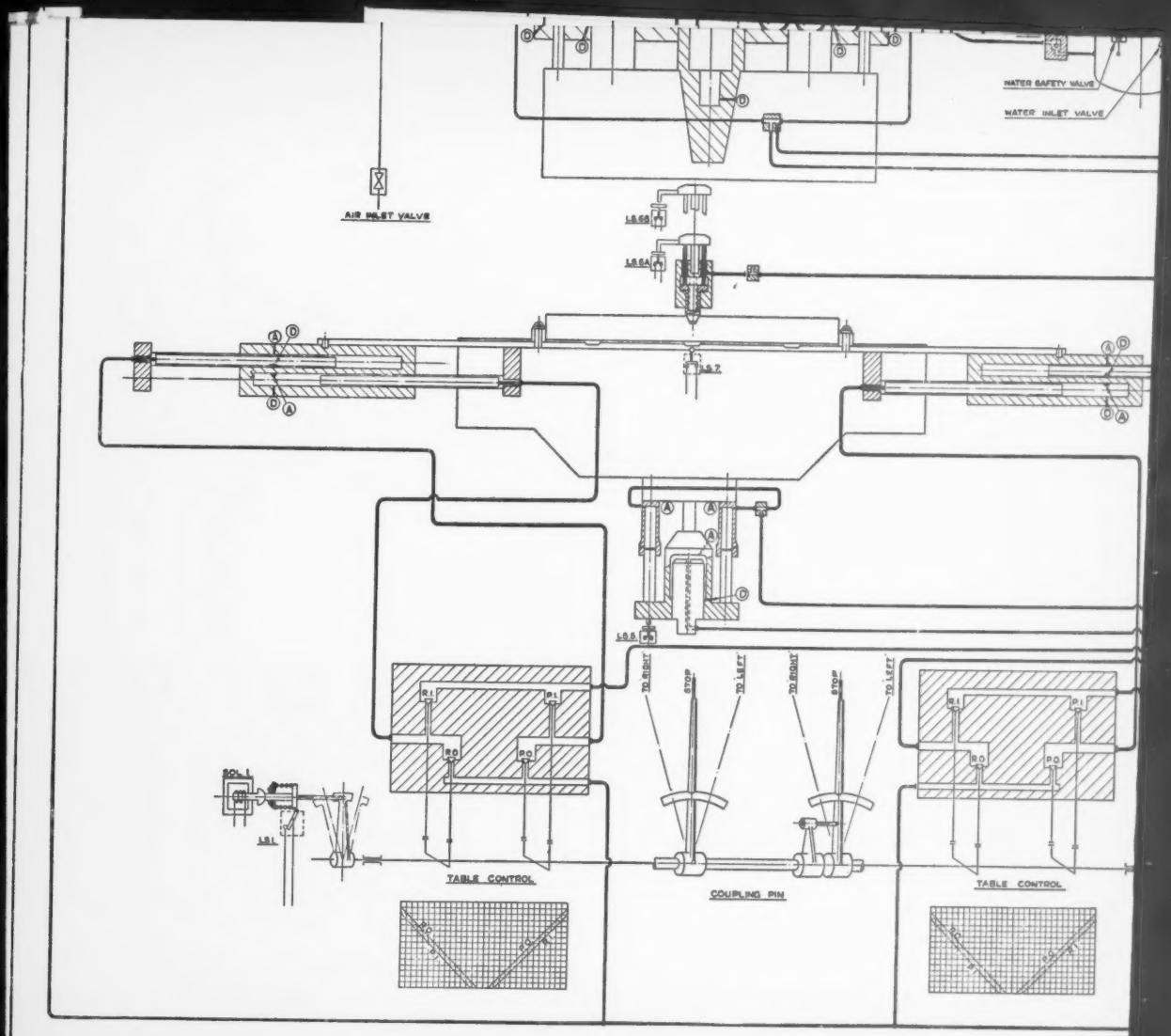
WATER SAFETY VALVE
WATER INLET VALVE
AIR INLET VALVE
LS 6B
LS 6A
LS 7
LS 5
TO RIGHT
STOP
TO LEFT
TO RIGHT
STOP
TO LEFT
SOL 1
LS 1
R1
R0
TABLE CONTROL
COUPLING PIN
TABLE CONTROL

Fig. 4

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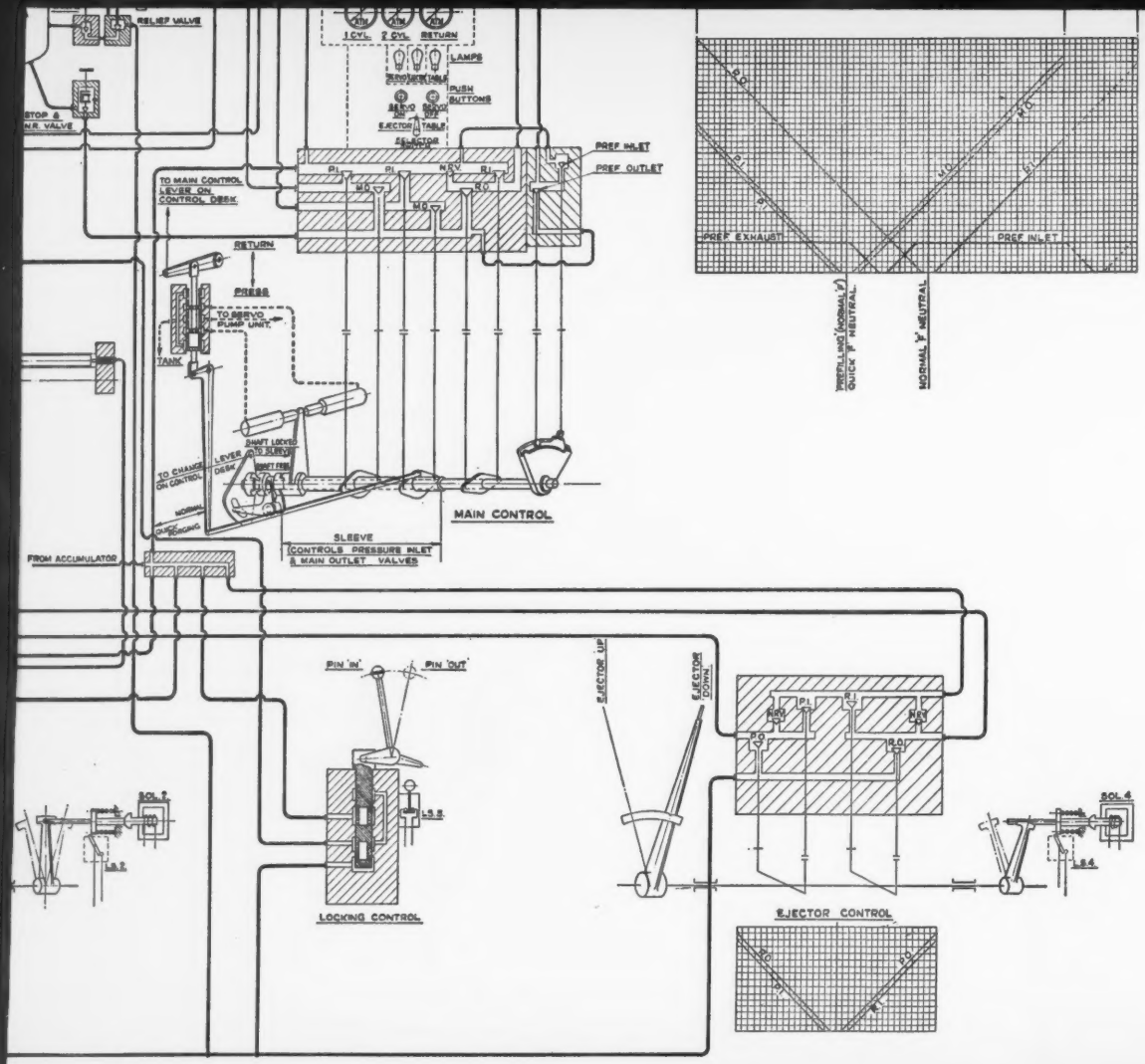

Typical control diagram for modern high-speed three-cylinder forging press

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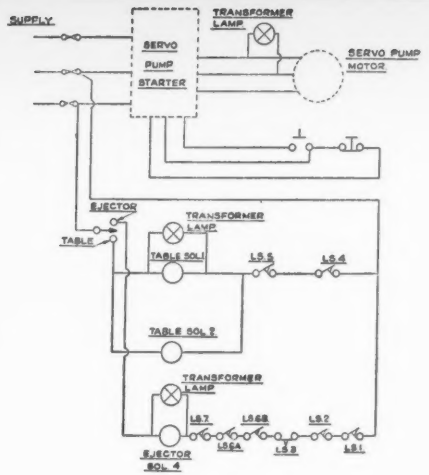


DIAGRAM OF CONNECTIONS

[*Elkan and Lewis*

[*To face p.* 206

XUM

HYDRAULIC CIRCUITS

Control Diagram for 3-Cylinder High-Speed Forging Press

Figure 4 shows a typical control diagram for an air-hydraulic accumulator-operated modern high-speed press. Three-pressure-stage selection is carried out by selecting the respective main inlet valves for an operation. For quick forging, only the pressure inlet and main outlet valves are used, the return cylinders being placed under constant accumulator pressure, thereby ensuring instantaneous response, as no time is required for pressure build-up in these to effect immediate reversal. This factor has proved invaluable, not only for high-speed planishing strokes in forging presses, but also in other special press applications, where the contact time between two dies had to be kept down to the minimum.

The servo control system gives sensitive and finger-light control through a reasonable degree of lever movement to the operator, who is no longer exposed to fatigue, and can concentrate wholly on the forging and on the co-ordination of his function with that of the crane or manipulator driver and forge-crew foreman.

When quick-forging, the prefilling valve is inoperative, the exhaust (i.e. return line from the main control block) is led back into the prefilling vessel, and the entire system works smoothly and without shock. By adequate care in the design of the hydraulic circuits, it is possible to have a system free from pipe breakages due to pressure surges and other shock effects.

A very thorough description of a typical modern oil control system has been given in detail elsewhere,[5] and will not be repeated here.

An important factor in hydraulic circuit performance is the construction and design of the valves. For the conditions under which a forging press works, the valve and pump equipment must be the most robust and least sensitive possible, since any power saving that might be achieved is rapidly offset if the expedients used are too sensitive and complicated and result in prolonged periods of breakdown. The well-known basic valve (Fig. 5*a*) has proved its reliability over many years. As shown in Fig. 5*a*, a large number of small throttle inlet holes were provided to give sensitive speed regulation over a large portion of the valve lift.

Table III

***K* FACTORS**

$$\text{Degree of valve opening} = \frac{\text{Area of holes uncovered}}{\text{Nominal valve area}}$$

Degree of Valve Opening	Approx. Value of *K* Factor
0·3	40–45
0·4	20–27
0·5	12–17
0·6	9–12
0·7	7– 9
0·8	6– 7
0·9	5– 6
1·0	4– 5

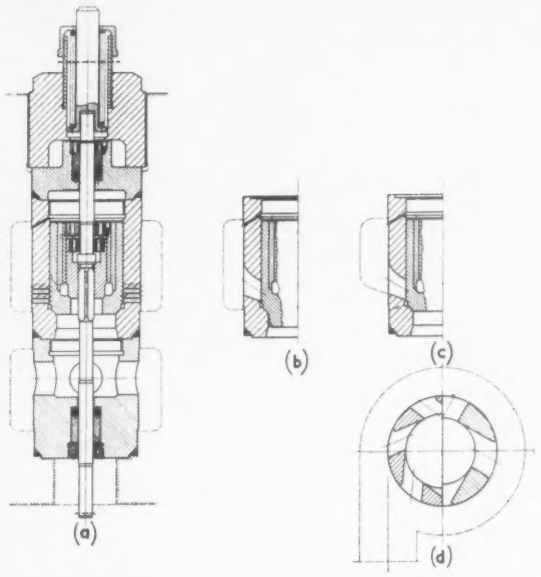


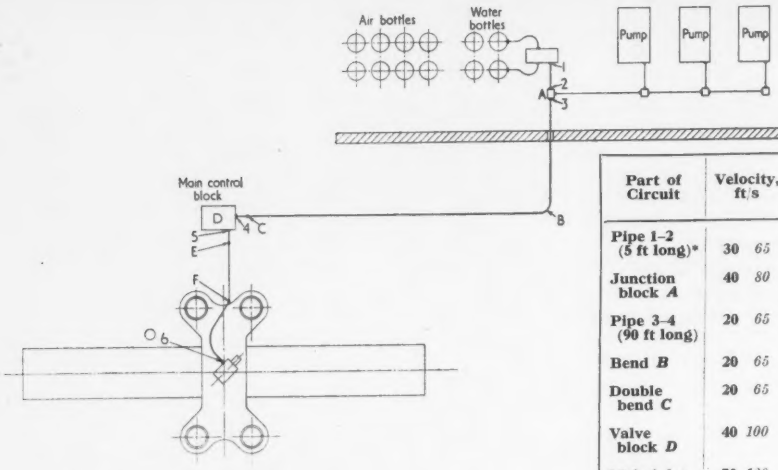
Fig. 5—Original valve showing various adaptations for special requirements

It is important that in forging presses the operator has sensitive control over the press movements; very sensitive control is needed on the one hand, but very full and free flow must be available when maximum speed and power are required simultaneously. Any modern servo-operated control gear therefore must give sufficient sensitivity of control, and yet be able without an undue arc of movement to give full bore opening when required.

To provide sensitive control in the initial stages of valve opening and free flow at full opening, modifications to the valve and seat shown in Fig. 5*b* are sometimes useful. To lower the *K* factors of this type of valve (*see* Table III), which can only be achieved by reducing the number of sudden changes in direction of flow and the avoidance of too much turbulence, the methods shown in Figs. 5*c* and *d* can, in special applications, be of value.

One of the most important features of hydraulic circuit design is the correct assessment of the friction losses through pipe lines and, above all, through valves and valve blocks. Figure 6 shows a deliberately oversimplified analysis of such losses between an accumulator station and the press cylinder.

Two different sets of velocities are taken through the pipes, valve, etc., and the effect of these assumptions on the pressure losses are shown. (The second set of velocity assumptions and the resulting figures are given in light type). The figures point clearly to the need for keeping down velocities in hydraulic systems; whereas on one set of assumptions the ' head lost ' is only about 5% of the available working pressure at the accumulator station, in the second instance the losses already amount to 25% of available



Part of Circuit	Velocity, ft/s	K Factor	Head Loss ft	Head Loss atm
Pipe 1–2 (5 ft long)*	30 *65*	0·24 *0·36*	3 *24*	0·09 *0·70*
Junction block *A*	40 *80*	1	25 *100*	0·74 *2·94*
Pipe 3–4 (90 ft long)	20 *65*	3·5 *6·4*	22 *423*	0·65 *12·45*
Bend *B*	20 *65*	0·4	3 *27*	0·09 *0·80*
Double bend *C*	20 *65*	0·8	5 *53*	0·15 *1·56*
Valve block *D*	40 *100*	2	50 *310*	1·50 *9·12*
Main inlet valve in block *D*†	70 *120*	5	380 *1120*	11·20 *33·00*
Pipe 5–6 (80 ft long)	25 *65*	3·5 *5·7*	34 *373*	1·00 *11*
Double bend *E*	25 *65*	0·8	8 *53*	0·24 *1·55*
Double bend *F*	25 *65*	0·8	8 *53*	0·24 *1·55*
		Total head lost	538 *2536*	15·90 *74·67*

* $K = \frac{4fl}{d}$ where f = friction coefficient, l = length of pipe, d = dia. of pipe

† Assume valve is fully opened. If valve is only half-opened, K = 15 say. Pressure losses through valve alone 34 and *99* atm.

Example: 2000-ton press, 300-atm system, upsetting speed 8 in./s. Volume flowing 8400 in³/s (assumed that 1 atm = 14·3 lb/in², working fluid water)

Fig. 6—Hydraulic losses between accumulator station and press cylinder

pressure, and on many of the older circuits losses of 50–70% of the available pressure were not unknown.

The hydraulic efficiency of a press circuit may be defined as:

$$\frac{\text{Effective pressure available on main ram of press at rated speed}}{\text{Pressure available at accumulator or pumps}};$$

its value for a modern press plant should be 85–90% at full speed, to obtain efficient utilization of a press.

ECCENTRICALLY APPLIED LOADS

In forging presses it is not possible to ensure that the reaction to the applied press power is exactly central to either the longitudinal or lateral axis of symmetry. It is not practicable for the forge crew to centre the ingot exactly at all times when forging, and when cogging down each bite unavoidably sets up a side force on the upper tool which results in an eccentric moment being applied to the press at each stroke. This causes wear in the locating bushes in this direction to a more pronounced degree than at right-angles to it, where often the press tonnage is applied at a far larger eccentricity, though at less frequent intervals.

As modern forging presses sometimes have to work at off-centre distances of 15 in., and on occasions even 20 in., the problem of how these forces are taken up in a press structure which must at all times remain substantially rigid and stand up to indefinite usage makes it essential to examine this question thoroughly.

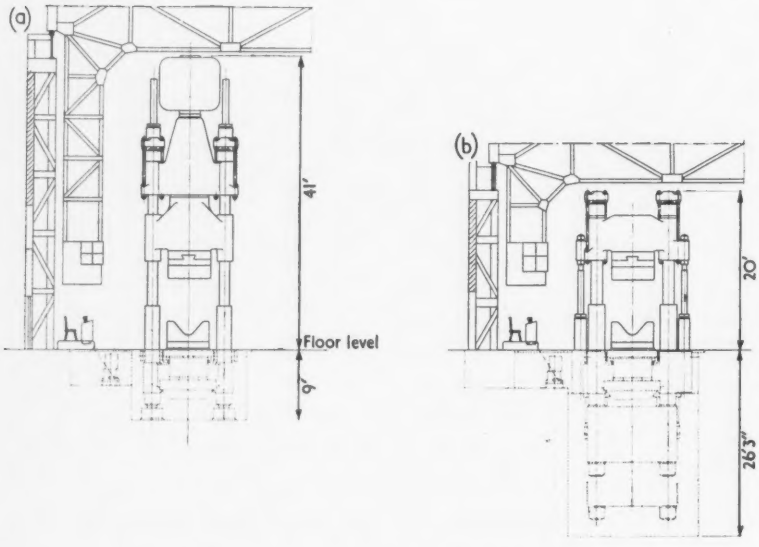


Fig.7—Alternative arrangements of 2000-ton forging press

Considerable bending moments are set up at the corners between upper and lower platens and the columns, and a knowledge of these is essential to proportion the press parts correctly.

Stone[2] indicated one approximate method of assessing the correct forces in an eccentrically loaded press. In Appendix V this method is compared with two further methods, which, though also not absolutely rigid in their assumptions, yield sufficiently accurate results to make them worthwhile. All these methods constitute a step forward from the simple direct 'load/area' calculations which used to be made for the press columns; the actual total stresses due to bending in press columns can easily be a multiple of the direct tension stress due to the nominal press load.

DEVELOPMENTS

At present much development work is being carried out on forging-press plant, and constant improvement in design and utilization of plants is in evidence.

At the time of writing, a special constant-speed valve is being manufactured which will find applications where constant pressing speed is required from accumulator circuits. Indicator readings and performance charts are being taken, and valve design is constantly under review in an endeavour to obtain the most efficient equipment consistent with simplicity and ruggedness which will remain the overriding requirement in forging plant.

The designer of press plants is often faced with special requirements. A recent example is shown in Figs. 7*a* and *b*, where a customer wished to install a 2000-ton press into the headroom of an existing shop. An inverted press construction of the 'pull-down' type indicated in Fig. 7*b* was therefore put forward; the advantage of this particular design, which may at first sight appear somewhat unorthodox, is the very narrow top crosshead which can be used and the low headroom required; on the other hand a deeper foundation is necessary, and the moving masses are considerably heavier. Figure 7*a* shows the same press but of orthodox construction, which has cheaper foundations, more headroom requirements, and a slightly less favourable accessibility for cranes and manipulator gear.

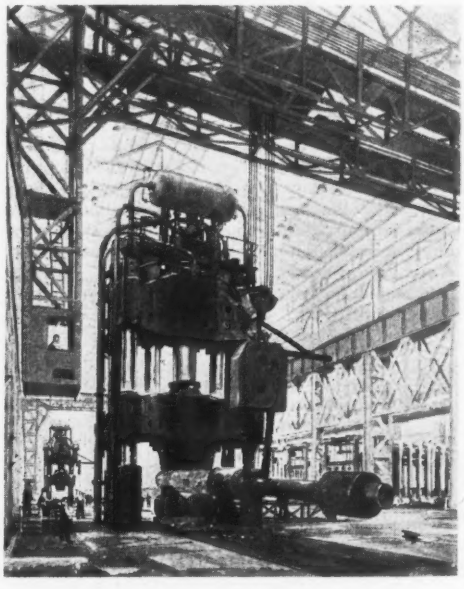


Fig. 8—2700-ton triple-cylinder forging press, with 1500-ton single-cylinder press in background

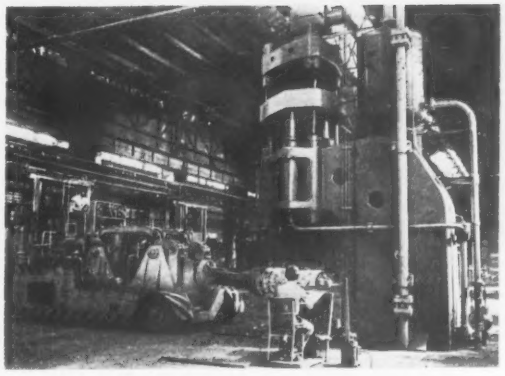


Fig. 9—600-ton C-frame press being served by mobile manipulator

CONCLUSIONS

Modern press plants demand high speeds of penetration and appropriate circuit design to ensure effective press utilization at such speeds. The need for the study of eccentric moments applied to forging presses is emphasized and two additional approximate methods of calculating the stresses set up under these conditions are given.

There are very strict limits to direct-drive applications, governed by the reasonable pump output that can be installed, based partly on first installation costs and space requirements. For large presses and high speeds of penetration the air-loaded hydraulic accumulator system alone can be considered.

Economies in power consumption, if they are achieved at the cost of what can be done per heat by presses working at two or three times the forging rates on air-hydraulic accumulator drive, give a totally false impression of efficiency. If a part can be forged with four reheats on a high-speed air-hydraulic accumulator driven press, which would require five or even six reheats when working at, say, one-half or one-third of such speeds, any power savings achieved have no bearing on the overall criterion, namely the efficient utilization of the press and entire plant.

Many of the problems touched upon in the paper are also associated with die-forging presses; it was, however, not possible for reasons of space to deal with this separately. A number of high-speed automatically operated steel die-forging presses have recently been installed; in the light-alloy field, for instance, two 12,000-ton presses have been working for many years in the U.K.; 35,000- and 50,000-ton presses were placed into operation in the U.S.A. earlier this year.

Figure 8 shows a 2700-ton triple-cylinder forging

Fig. 10—Two typical air-hydraulic accumulator stations

press in the foreground and a 1500-ton single cylinder press in the background, both installed in recent years. Figure 9 shows a 600-ton C-frame press with modern servo-operated control gear, served by a mobile manipulator. Figures 10*a* and *b* show two typical air-hydraulic accumulator stations.

Acknowledgments

The authors wish to thank their Directors for permission to publish this paper, and express special thanks to their colleague, Mr. T. C. Lindop, A.M.I. Mech.E., for his contribution of Appendix V, and also for his patient work in checking this paper.

APPENDIX I

Availability of Maximum Power at Forging Press

In explaining the often expressed statement that a forging press only exerts about two-thirds of its rated capacity when actually forging with a certain speed of penetration, it must be assumed that this is based on the relationship from which it can be shown that maximum power is exerted at the end of a pipe line when one-third of the available head is lost in friction.

Let H = pressure head of water available at accumulator station
h_f = pressure head of water lost in friction in pipes, valves, etc., between accumulator station and forging press cylinders
k_1, k_2 = constants
v = nominal pipe velocity.
Then $h_f = k_1 v^2$
H_e = effective head available at press cylinders
$= (H - k_1 v^2)$
Power available in press cylinders $= k_2 v\,(H - k_1 v^2)$
Differentiating w.r. to v and equating to 0 for a maximum,

$$\frac{\mathrm{d}\,(\text{power})}{\mathrm{d}v} = 0 = H - 3\,k_1 v^2$$

$$\therefore\ k_1 v^2 = \frac{H}{3}$$

i.e. when the losses are one-third of available head, maximum power is available at the press.

The above, although giving the optimum conditions for power availability, does not represent the optimum utilization of a forging press, which should be capable of exerting up to 85–90% of its rated power at penetration speeds of up to 6–10 in./s. It is most probable that the 33% or even 50% friction losses that occur in presses, even at moderate speeds, are due to insufficient care in circuit design and the adherence to old rules of thumb, which so often cause slowing down, even to the point of stalling, of a press, when it should be exerting an appreciable part of its rated tonnage at a definite penetration speed.

[*Note:* Although the above remarks refer to losses between an accumulator station and a press, the problem of pipe and valve losses and the efficiency of transmission exists also with the direct-drive system.]

APPENDIX II

Effects of Compressibility

In calculations of hydraulic circuits the effect of liquid compressibility cannot be ignored. This becomes of special significance when considering the pump output required when quick-forging or planishing. Thus, if 50 or 60 strokes are required per minute at the full press rating, on direct drive the pressure has to be built up from zero to working pressure this number of times per minute, and it will be found that, depending on the position of the main rams (i.e. the length of extended working stroke at which the press is working) and the length and bore of the pipe line—in short, the liquid volume that has to be compressed—5–15% or more may be required in additional pumping capacity to compress the extra volume in the time periods required. On accumulator drive the problem is eased as the additional volume can be pumped during the entire working period, and furthermore only the pipe line between the main control valve block and the press cylinder and the extended stroke volume of the cylinders at any instant need be compressed, not the entire piping system between press and pump house.

Expressions for the compressibility of different oils and water and their variation with pressure can be found in the standard literature on hydraulics:

Let: K = bulk modulus of liquid
$\mathrm{d}P$ = increase in pressure
$\mathrm{d}V$ = resultant decrease in liquid volume
V = original volume

$$\text{Then } K = \frac{\mathrm{d}P}{\mathrm{d}V/V} = \frac{\text{(Volumetric) stress}}{\text{(Volumetric) strain}}$$

$$\text{Integral compressibility is defined as } \frac{1}{K} = \frac{\mathrm{d}V/V}{\mathrm{d}P}$$

It is often expressed as a ratio 'per atmosphere' or per lb/in²; alternatively, it is stated as a percentage per 1000 lb/in².

The bulk modulus for water at pressures used in forging-press systems can generally be taken as 300,000

lb/in² (i.e. compressibility 49 × 10⁻⁶ per atm or 0·334% per 1000 lb/in²).

The bulk modulus for the lighter type of oils as used in high-speed multi-plunger pumps may be taken as 283,000 lb/in² (i.e. compressibility 52 × 10⁻⁶ per atm or 0·354% per 1000 lb/in²); however, the thicker oils are somewhat more compressible.

If figures are calculated for the diametral increase of the press cylinders and the pipe bores due to the hoop stresses set up by the fluid pressure, an overall bulk modulus of about 200,000 lb/in² (i.e. compressibility 73·5 × 10⁻⁶ per atm or 0·5% per 1000 lb/in²) is applicable.

[*Note:* If a pressure P be applied to a liquid system, the strain energy stored = $(P^2/2K) \times$ liquid volume. This value is often very high and, to obtain a full picture of the total energy stored each time the pressure in the cylinders is built up, it is necessary to add the strain energy due to bending of the upper and lower cross structures, that of tension in the columns, and of the cylinder walls under hoop stress. The total value of these stored energies may under certain circumstances be as much and more than the strain energy of the liquid, and the combined strain energy in all instances represents a very large quantity which has to be released upon decompression, before each return stroke begins, in time periods that are at best only very small fractions of a second.]

APPENDIX III

Water Hammer Effects

In many old press plants inadequate care was taken in the design of hydraulic circuits, and excessive pipeline velocities were often and are still being used. When pipe lengths are long and times of valve closure are short, the effect of water hammer must be taken into account; failure to do this results in shocks in the pipe line, and often considerable damage resulting from excessive pressures occurs. Very often loud reports can be heard whenever valves are suddenly closed or opened, pointing to a failure in guarding against water hammer. Hydraulic circuits of modern presses must be examined in the light of this, especially as more modern valve gears work at very high speed, sometimes involving 120 or even more valve reversals per minute.

The subject of water hammer has been fairly exhaustively examined, although some more experimental test data for high-pressure systems would be of value.

Let w = specific weight of liquid
P = excess pressure rise in pipe line due to water hammer
v = velocity of moving water before start of complete valve closure
d = nominal diameter of pipeline
t = wall thickness of pipe
σ = Poisson's ratio.

If as a first step the time of valve closure is assumed to be instantaneous, then the maximum sudden excess pressure rise P is given by the formula, derived by equating the kinetic energy of the water flowing in the pipe line to the strain energy of the water and pipe walls:

$$P = v\sqrt{\left[\frac{w}{g\left\{\frac{1}{K} + \frac{d}{4tE}(5 - 4\sigma)\right\}}\right]} \quad \ldots\ldots\ldots\ldots(1)$$

If the elasticity of the pipe walls is ignored

$$P = v\sqrt{\frac{wK}{g}} \quad \ldots\ldots\ldots\ldots(2)$$

If liquids were incompressible, which is far from being the case, and only the elasticity of the pipe metal were to absorb the energy,

$$P = v\sqrt{\frac{w4tE}{gd(5 - 4\sigma)}} \quad \ldots\ldots\ldots\ldots(3)$$

Equation (3) has no meaning, but if actual valves are inserted in it and a comparison is made with equations (1) and (2), it does show the measure of the contribution of liquid compressibility on the limitation of pressure rise.

Example: A 4-in. nominal dia. pipe line has ½-in. wall thickness; velocity of moving water column 25 ft/s; find excess pressure rise due to instantaneous valve closure.

Take: $E = 3 \times 10^7$ lb/in², $w = 62{\cdot}4$ lb/ft³
$K = 3 \times 10^5$ lb/in², $\sigma = 0{\cdot}25$

By substitution in equation
(1) $P = 1515$ lb/in² $(= 60{\cdot}8\ v)$
(2) $P = 1590$ lb/in² $(= 63{\cdot}8\ v)$
(3) $P = 5590$ lb/in² $(= 223\ v)$

where v is in ft/s and P in lb/in².

Equation (1) shows that the pressure rise due to water hammer for instantaneous closure conditions = $60{\cdot}8\ v$. It can thus be seen why, apart from friction loss considerations, it is important not to have excessive velocities in long mains when times of valve closure are short. Equation (2) shows that the effect of pipe elasticity is not so appreciable, and equation (3) indicates to a marked degree the effect of fluid compressibility in limiting the magnitude of the pressure rise.

It can be shown that the pressure rise as given by equation (1) is the maximum that can occur:

If the time of valve closure is T s, then at a very rough approximation the actual pressure rise is

$$\left(\frac{t}{T} \times P\right) \ldots\ldots(4)$$

where t = time for pressure wave to travel length of pipe and back $(= 2l/v_s)$
l = length of pipe line
v_s = velocity of sound in water.

Equation (4) only holds for $T > t$; if $t > T$, equation (1) is correct.

APPENDIX IV

Pipe and Valve Friction

A correct assessment of the friction losses through pipes, valves, and other constrictions in a hydraulic circuit for a fast-acting press is of primary importance.

In general, all pipe flow encountered in hydraulic press circuits using water as the fluid medium is "turbulent."

[*Note:* Reynolds number R should be in excess of 4000; streamline flow ceases at an R value of 2000 and the transition stage ends at a value of approximately 4000.]

It is thus only necessary to obtain the correct friction factor f in the formula for head loss:

$$h = \frac{v^2}{2g} \cdot \frac{fl}{d} \ldots\ldots(a)$$

where v = velocity of flow in pipe.
d = pipe dia.
l = length of pipe.

Values for f are given in the standard textbooks on hydraulics, or they may be read off a diagram of the Stanton type which gives the value of friction factors as a function of Reynolds number, and latterly with an allowance for the relative roughness of pipes.

When oil is the working fluid, great care has to be taken in the evaluation of the friction coefficient. At first the Reynolds number should be determined from such formula as

$$R = \frac{7750vd}{\nu}$$

where v = pipe velocity, ft/s
d = pipe dia., in.
ν = kinematic viscosity, cS

If R is less than 2000 and flow is streamline, equation

(*a*) can be used, but the value of $f = 64/R$ and it is independent of surface condition of pipe. If R is more than 2000, equation (*a*) is also used, but the value of f must be determined as previously indicated for water.

Sometimes the equation $f = 0{\cdot}3164/R^{0{\cdot}25}$ is used for evaluating the friction coefficient.

When flow is streamline, the friction head loss is proportional to the velocity, but when turbulent, to the square of the velocity; for streamline flow, the friction loss varies directly with the viscosity, and in turbulent flow with approximately the fourth root of viscosity. This is therefore a strong point in favour of the use of water or of low-viscosity oils.

The losses of head when fluids pass through right-angle bends, sudden enlargements and contractions, and other obstructions are also given in published literature. Generally, for fittings and valves, the head loss is expressed as

$$h = K\frac{v^2}{2g}$$

where K = loss factor (i.e. a dimensionless parameter)
v = velocity through part.

Table III gives values of K factors for different degrees of valve opening for valves as shown in Fig. 5*a*. Although tests made in the determination of these factors were not too sensitive by laboratory standards, it was established that:

(i) The value of the K factors for any degree of valve opening is substantially constant, irrespective of the size and arrangement of holes in the valve cage (i.e. the K factor is a function of the shape of the valve passages, rather than of their size)

(ii) The K factor increases with the number of changes of direction of flow causing additional turbulence and eddy losses

(iii) Surface friction and surface condition affect the K factor

(iv) All geometrically similar valves have identical K factors, variations being due to surface conditions and the absence of absolute similarity.

Lonsford[6] carried out a series of tests for a range of Reynolds number from 10,000 to 600,000. The dimensionless parameters of R and K were plotted, and it was found that the head loss in all valves tested varied approximately as the square of the nominal valve velocity, the value of K remaining substantially constant as R values were increased. The figures plotted enable the value of K to be found (for any of the valves tested), regardless of what fluid is flowing through the valves, as long as the viscosity, density, and velocity of the fluid are given. Thus the head loss can be computed from the value of K taken from the curves, covering the complete range of fluids at varying temperatures and densities.

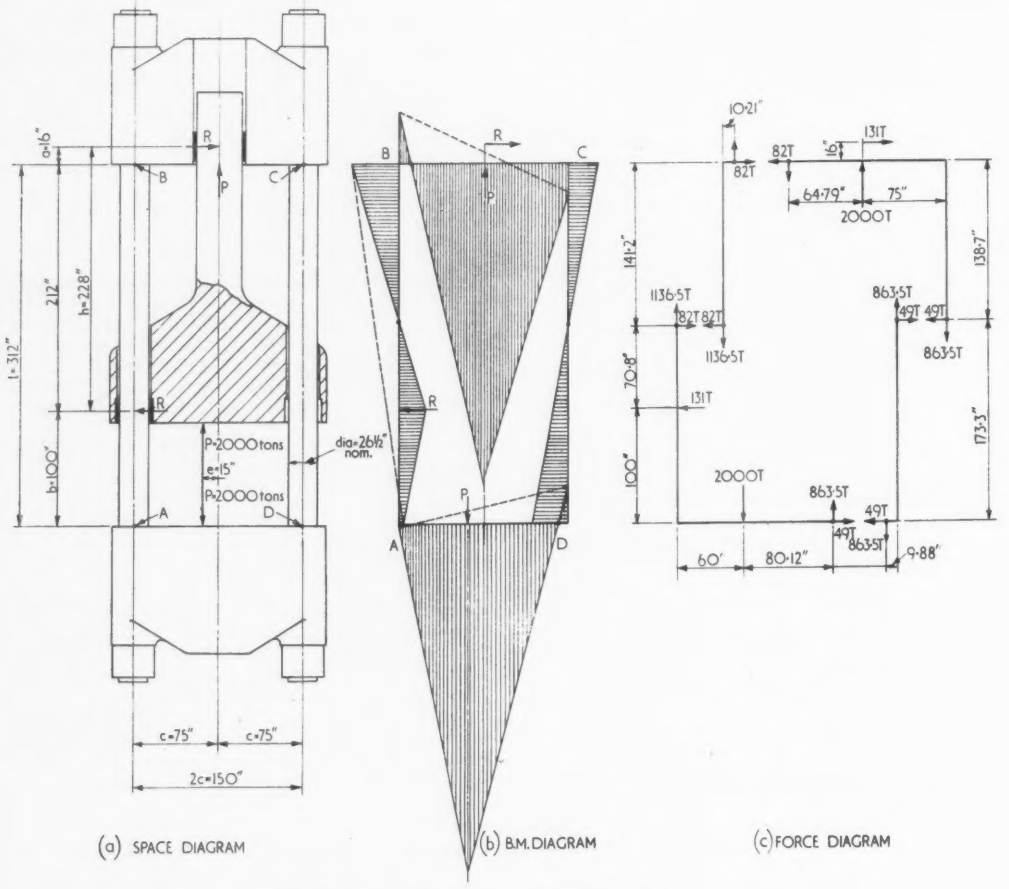


Fig. 11—Structural analysis of press frames

APPENDIX V

Effect of Eccentric Loading on Press Structures

Eccentric loads placed on press structures can be dealt with in one of three ways:

(1) By absorbing the applied moment within the press structure
(2) By providing hydraulic balancing rams which in effect act as external 'props' or resistances to the applied moment
(3) By the provision of a mechanical device such as a torsion bar, to counteract the eccentrically applied load.

This appendix is intended to deal solely with an example of case (1), i.e. all moments resisted within the structure of the press.

Three methods are indicated; the first is that generally known as the 'moment distribution' method. It is simple in use and is capable of being abbreviated, yet still give results sufficiently accurate for design purposes. The second method is an approximation based on method 1, and method 3 is that developed by Stone.[2] To make the results obtained by all three comparable, the assumptions made by Stone were adopted; thus the lateral reaction was taken on one column pair only, and not on both, for Stone stated that in his opinion this reaction could not be relied upon to be equally divided between both column pairs as a result of advanced wear on, or the 'dropping out' of, a set of column guide bushes. This view is not strictly shared, but to keep conditions identical the same assumption was retained; point loading is also assumed, which does not quite represent true conditions.

In method 1 the distribution was stopped after four operations, as the changes in the fixing moments were becoming very small. The method is capable of evaluation to any degree of accuracy simply by continuing the distributions.

Method 2 shows the results obtained when the upper and lower platens of the press frame are regarded as rigid members.

From the summary of results it is seen that method 1 yields the highest maximum corner moment. This is due to the fact that it takes into account the curvature of the upper and lower platens. Methods 2 and 3 yield approximately the same figures for the maximum corner moment, about 10% below that given by method 1; this is correct in view of the fact that both methods 2 and 3 assume rigid upper and lower platens. The symbols used are defined in the space diagram of Fig. 11*a*; this shows a moving crosshead guided on the column and having its second reaction within the upper platen. This method of taking the eccentric moment is that used in the presses of Figs. 2 and 3, and it was also one of the cases for which Stone had derived formulæ. One of the shortcomings of method 3 is the fact that each press design requires the evaluation of a new set of formulæ. Methods 1 and 2 can, however, be applied to any press design, once they have been fully understood and may, therefore, be more valuable for use in the press design office.

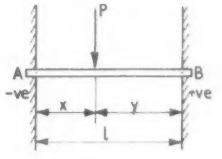
The sway deflection given by method 3 is the highest, methods 1 and 2 yielding a value 10% lower. This is largely the result of the fact that the deflection equation used by Stone is derived from expressions which do not contain the reduction in moment on the frame due to the top horizontal force R acting within the upper platen; when this correction is applied to this figure the value of the sway deflection comes down from 0·4 in. to 0·372 in. On the other hand, methods 1 and 2 do not take into account the unequal extension of the columns (0·013 in.), and when these methods in turn are corrected for this, they yield a value for the side sway of 0·373 in.

All three methods can enable the corner moment as well as the side sway to be evaluated. For the press structure shown, it is possible to draw a bending moment diagram as given in Fig. 11*b* from the results obtained, and in turn it is possible to draw the actual force diagram as shown in Fig. 11*c*.

Without any of the above analyses, the direct tensile stress per column would nominally have been 1·82 ton/in². By using any of the three methods, the maximum direct tensile stress per column is in fact 2·07 ton/in². If the maximum corner moment is taken as 11,600 ton-in., the maximum stress due to bending is in fact, 6·35 ton/in². Thus, the maximum combined stress per column is found to be 8·42 ton/in². Thus by the use of any of the three methods outlined, it is possible to make an accurate assessment of the total stresses in a column and obtain a correct picture of the forces acting.

Explanation of Symbols

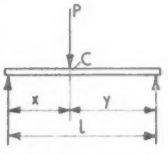
BM = Bending moment
FM = Fixing moment
E = Young's modulus
I = Moment of inertia
a, b, c, e, h, l, P, R as defined on space diagram (Fig. 11*a*).



$$FM_{AB} = \frac{Pxy^2}{l^2}$$

= Fixing moment at end A of built-in beam AB

$$FM_{BA} = \frac{Px^2y}{l^2}$$



$$BM_C = \frac{Pxy}{l}$$

= Bending moment at C

$$\text{Free } BM_{BC} = \frac{2000 \times 150}{4} = 75{,}000 \text{ ton-in.}$$

$$FM_{BC} = FM_{CB} = \frac{2000 \times 150}{8} = 37{,}500 \text{ ,,}$$

$$\text{Free } BM_{AD} = \frac{2000 \times 60 \times 90}{150} = 72{,}000 \text{ ,,}$$

$$FM_{AD} = \frac{72{,}000 \times 90}{150} = 43{,}200 \text{ ,,}$$

$$FM_{DA} = \frac{72{,}000 \times 60}{150} = 28{,}800 \text{ ,,}$$

Pe = Eccentric moment

$Pe = Rh$ (considering half frame $P = \frac{4000}{2}$ tons)

$$R = \frac{2000 \times 15}{228} = 131 \text{ tons} = \text{reaction on column and in upper platen}$$

$$\text{Free } BM_{AB} = \frac{131 \times 100 \times 212}{312} = 8900 \text{ ton-in.}$$

$$FM_{AB} = \frac{8900 \times 212}{312} = 6050 \text{ ,,}$$

$$FM_{BA} = \frac{8900 \times 100}{312} = 2850 \text{ ,,}$$

$$FM_{CD} = FM_{DC} = 0$$

Method 1

	A		B		C		D	
I of member, in^4	465,000	24,200		465,000		24,200		465,000
Length of member, in.	150		312	150		312		150
I/length, in^3	3100		77·5	3100		77·5		3100
Distribution factor	0·975	0·025	0·025	0·975	0·975	0·025	0·025	0·975
Fixed end moment	−43,200	+6050	−2850	+37,500	−37,500	0	0	+28,800
	+36,221	+ 929	− 866	−33,784	+36,562	+ 938	−720	−28,080
Operation 1	− 6979	+6979	−3716	+ 3716	− 938	+ 938	−720	+ 720
	−14,040	− 433	+ 465	+18,281	−16,892	− 360	+469	+18,111
	+14,111	+ 362	− 469	−18,277	+16,821	+ 431	−465	−18,115
Operation 2	− 6908	+6908	−3720	+ 3720	− 1009	+1009	−716	+ 716
	− 9058	− 235	+ 181	+ 8411	− 9139	− 233	+216	+ 7056
	+ 9061	+ 232	− 215	− 8377	+ 9138	+ 234	−182	− 7090
Operation 3	− 6905	+6905	−3754	+ 3754	− 1010	+1010	−682	+ 682
	− 3545	− 108	+ 116	+ 4569	− 4189	− 91	+117	+ 4531
	+ 3562	+ 91	− 117	− 4568	+ 4173	+ 107	−116	− 4532
Operation 4	− 6888	+6888	−3755	+ 3755	− 1026	+1026	−681	+ 681

'*Propping moment*' = Out-of-balance moment existing, assuming frame not free to 'sway' sideways
= + 6888 − 3755 + 1026 − 681 = + 3478

Total moment acting on frame = Eccentric moment applied, less moment applied within upper platen
= − (2000 × 15) + (131 × 16) = − 27,904

Total correcting moments = − 27,904 − 3478 = − 31,382

$$\textit{Correcting moment/corner} = \frac{-31{,}382}{4} = -7845$$

Summary

	A	B	C	D
	+6888	−3755	+1026	− 681
	−7845	−7845	−7845	−7845
Final corner moments (actual)	− 957	−11600	−6819	−8526

Check: The sum of actual corner moments must be equal to total moment acting on the frame: − 957 − 11,600 − 6819 − 8526 = − 27,902

Method 2

$$R = 131 \text{ tons} \left(= \frac{Pe}{h}\right)$$

Free BM_{AB} = 8900 ton-in.

FM_{AB} = + 6050 ton-in.

FM_{BA} = − 2850 ton-in.

$FM_{CD} = FM_{DC} = 0$

'*Propping moment*' = + 6050 − 2850 = + 3200 ton-in.

Total moment acting on frame = − (2000 × 15 in.) + (131 × 16 in.) = − 27,904 ton-in.

Total correcting moment = − 27,904 − 3200 = − 31,104 ton-in.

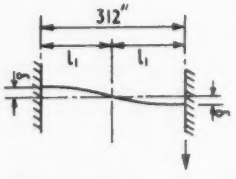
$$\textit{Correcting moment/corner} = -\frac{31{,}104}{4} = -7776 \text{ ton-in.}$$

Summary

A	B	C	D
+6050	−2850	0	0
−7776	−7776	−7776	−7776
−1726	−10626	−7776	−7776

The sum of these corner moments
= − 1726 − 10,626 − 7776 − 7776
= − 27,904 ton-in.

Sway Deflection (Δ)



$$\textit{Side force on frame} = \frac{27{,}904}{312} = 89{\cdot}5 \text{ tons}$$

Side force per column = 44·75 tons

$$\textit{Deflection } \Delta = (2\delta) = \frac{2 \times 44{\cdot}75 \times 156^3}{3 \times 13{,}000 \times 24{,}200} = 0{\cdot}36 \text{ in.}$$

Method 3

Formulæ used by Stone.[2] Symbols Defined in Space Diagram (Fig. 11*a*)

Corner moment at

$$A = \frac{Pe}{4\left(\frac{h}{l}\right)}\left\{1 - 4\left(\frac{b}{l}\right) + 5\left(\frac{b}{l}\right)^2 - 2\left(\frac{b}{l}\right)^3\right\}$$

$$= \frac{30{,}000}{4 \times \frac{228}{312}}\left\{1 - 4\left(\frac{100}{312}\right) + 5\left(\frac{100}{312}\right)^2 - 2\left(\frac{100}{312}\right)^3\right\} = 1690 \text{ ton-in.}$$

$$B = \frac{Pe}{4\left(\frac{h}{l}\right)}\left\{1 + \left(\frac{b}{l}\right)^2 - 2\left(\frac{b}{l}\right)^3\right\}$$

$$= \frac{30{,}000}{4 \times \frac{228}{132}}\left\{1 + \left(\frac{100}{312}\right)^2 - 2\left(\frac{100}{312}\right)^3\right\} = 10{,}650 \text{ ton-in.}$$

$$C = \frac{Pe}{4\left(\frac{h}{l}\right)}\left\{1 - 3\left(\frac{b}{l}\right)^2 + 2\left(\frac{b}{l}\right)^3\right\}$$

$$= \frac{30{,}000}{4 \times \frac{228}{312}}\left\{1 - 3\left(\frac{100}{312}\right)^2 + 2\left(\frac{100}{312}\right)^3\right\} = 7770 \text{ ton-in.}$$

$D = 7770$ ton-in.

BM in column AB at point of application of R

$$= \frac{Pe}{4\left(\frac{h}{l}\right)}\left\{1 - 2\left(\frac{b}{l}\right) + 5\left(\frac{b}{l}\right)^2 - 8\left(\frac{b}{l}\right)^3 + 4\left(\frac{b}{l}\right)^4\right\}$$

$$= \frac{30{,}000}{4 \times \frac{228}{312}}\left\{1 - 2\left(\frac{100}{312}\right) + 5\left(\frac{100}{312}\right)^2 - 8\left(\frac{100}{312}\right)^3 + 4\left(\frac{100}{312}\right)^4\right\}$$

$= 6680$ ton-in.

Sway deflection (Δ)

$$= \frac{Pel^3}{24hEI}\left\{1 - 3\left(\frac{b}{l}\right)^2 + 2\left(\frac{b}{l}\right)^3\right\}$$

$$= \frac{30{,}000 \times 312^3}{24 \times 228 \times 13{,}000 \times 24{,}200}\left\{1 - 3\left(\frac{100}{312}\right)^2 + 2\left(\frac{100}{312}\right)^3\right\}$$

$= 0{\cdot}4$ in.

Direct load in column AB

$$= \frac{P}{2}\left\{1 + \frac{e}{c}\left(\frac{a}{h} + \frac{1 - [b/l]^2}{2\,[h/l]}\right)\right\}$$

$$= 1000\left\{1 + \frac{15}{75}\left(\frac{16}{228} + \frac{1 - [100/312]^2}{2[228/312]}\right)\right\} = 1136{\cdot}5 \text{ tons}$$

$CD = 863{\cdot}5$ tons.

Summary

	Method 1	Method 2	Method 3
Final corner moment at A	957 ton-in.	1726 ton-in.	1690 ton-in.
,, ,, ,, ,, B	11,600 ,,	10,626 ,,	10,650 ton-in.
,, ,, ,, ,, C	6819 ,,	7776 ,,	7770 ,,
,, ,, ,, ,, D	8526 ,,	7776 ,,	7770 ,,
BM in column AB at point of application of R	5807 ,,	6649 ,,	6680 ,,
Direct load in column AB	1136·5 tons	1137 tons	1137 tons
,, ,, ,, ,, CD	863·5 ,,	863 ,,	863 ,,
Sway deflection	0·36 in.	0·36 in.	0·40 in.

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4. *Ibid.*, 1952, vol. 171, pp. 413–419.
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6. W. M. Lonsford: *Ill. Univ. Eng. Exper. Sta.*, Bull. No. 340.
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N E W S

Announcements and News of Science and Industry

IRON AND STEEL INSTITUTE

Mr. R. A. Ronnebeck

Mr. R. A. Ronnebeck, a member of the staff of The Iron and Steel Institute for 17 years, died in hospital on 23rd December, 1955, after a long illness, aged 57. Mr. Ronnebeck, whose home town was Middlesbrough, was connected with the iron and steel industry in England and Sweden for a number of years before becoming an abstractor, first with I.C.I. Ltd. and later with the Bristol Aeroplane Co., Ltd. He joined the Institute's staff in the same capacity in 1938, and had been responsible in recent years for the preparation and classification of the abstracts published in the *Journal*. His interests were varied, and he was a member of the Newcomen Society and of the Royal Institute of International Affairs.

NEW YEAR'S HONOURS

Sir **John Morrison,** Chairman of the Iron and Steel Holding and Realization Agency, has been created G.B.E.

Mr. **Gerald Steel,** General Managing Director of the United Steel Companies Ltd., has been created a C.B.E.

Mr. **H. Parish,** Chief Engineer of Henry Wiggin and Co., Ltd., has been awarded the O.B.E.

Mr. **J. S. Curphey,** Steelplant Manager at the Lackenby Works of Dorman Long (Steel) Ltd., has been awarded the M.B.E.

NEWS OF MEMBERS

Mr. **A. R. Bailey** has resigned his position as Senior Lecturer in Metallurgy at the Constantine Technical College, Middlesbrough, and has been awarded a Turner and Newall Research Fellowship at the Royal School of Mines, London.

Mr. **U. G. Bhat** has been awarded the degree of Ph.D. in Metallurgical Engineering at Lehigh University and has joined the Research and Development Laboratories of the Crucible Steel Company, Pittsburgh.

Mr. **H. Christiansen** has taken up an appointment with Christiania Spigerverk, Oslo.

Dr. **H. E. Davis** has resigned his position as Chief Metallurgist to the Central Electricity Authority to take up a post with Richardsons Westgarth Ltd. on nuclear power production.

Mr. **N. F. Eaton** has received the degree of Ph.D. in the University of Wales and has joined the Research Department of the Metropolitan-Vickers Electrical Co., Ltd., Manchester.

Mr. **W. Geary,** Works Manager (Services) of the Appleby-Frodingham Steel Co., Scunthorpe, has relinquished his present responsibilities to take over special duties in management studies and statistics for the General Works Manager.

Mr. **P. F. Hancock,** Research Manager of Birlec Ltd., has been appointed a director of the Company.

Mr. **M. G. Hipkins** has resigned his appointment with the U.K. Atomic Energy Authority, Sellafield, to take up the post of Senior Metallurgist with British Oxygen Engineering Ltd., Edmonton.

Mr. **W. Jackson,** formerly Melting Shops Manager, Appleby-Frodingham Steel Co., Scunthorpe, has been appointed Works Manager (Steel).

Mr. **E. Klein,** hitherto Assistant General Works Manager (Production), has been appointed Development Manager (Development, General Engineering and Operational Research) of the South African Iron and Steel Industrial Corporation Ltd.

Dr. **C. M. Krüger,** hitherto General Works Manager, has been appointed General Manager of the South African Iron and Steel Industrial Corporation Ltd., and will be responsible for all the activities of the Corporation.

Sir **Ernest Lever** has been elected President of the British Iron and Steel Federation.

Mr. **W. E. Mahin** has resigned his position as Vice-President and Director of Research of the Hunter Engineering Company.

Sir **Andrew McCance** has been appointed President-elect of the British Iron and Steel Federation.

Mr. **M. Nelson** retired on 31st December, 1955, from his position as Technical Representative with Websters, Sheffield.

Mr. **C. M. Slocombe** has been elected to the Board of Directors of Samuel Fox and Co., Ltd.

Obituary

Sir **James Dunn,** BT., President and Chairman of Algoma Steel Corporation, on 1st January, 1956.

Mr. **Edgar Charles Evans,** on 17th December, 1955.

Monsieur **André Gosselin,** President of Davum Exportation, Paris, on 17th December, 1955.

E. C. Evans, B.Sc., F.R.I.C., F.Inst.F.

As recorded in the News of Members column the death occurred at his home in Stanmore, Middlesex, on 17th December, 1955, of Mr. Edgar Charles Evans.

Mr. Evans was elected a member of The Iron and Steel Institute in 1923 and was a founder of the Institute of Fuel, of which he was a Fellow. He joined the National Federation of Iron and Steel Manufacturers as Fuel Officer in 1924 and was Technical Secretary of the Iron and Steel Industrial Research Council from its formation in 1929 until it was reorganized as B.I.S.R.A. in 1945. During the same period he became Head of the Technical Department of the British Iron and Steel Federation, carrying out the functions of Director of Research. In the early years he played a major part in demonstrating to industry the advantages of fuel economy and of co-operative research.

In 1926, in association with Professor R. E. Wheeler, Mr. Evans had founded the Midland Coke Research Committee and encouraged the formation of other district coke research committees, which have now become the

British Coke Research Association. He was responsible for starting the Blast-furnace and Open-hearth Conferences, which continue today. On his retirement from the British Iron and Steel Federation in 1945 he became a consultant to a range of industries.

Mr. Evans had a notable quality of inspiring enthusiasm and the ability to see that research was carried out even when funds were limited. He leaves a wife and son.

IRON AND STEEL ENGINEERS GROUP

The Thirtieth Meeting of the Iron and Steel Engineers Group will be held at the Offices of the Institute, 4 Grosvenor Gardens, London, S.W.1, on Thursday, 15th March, 1956. The Meeting will be devoted to the presentation and discussion of a paper on "*Modern Forging Presses and their Control*," by Mr. R. M. L. Elkan and Mr. J. T. Lewis.

The morning session, from 10.30 A.M. to 12.45 P.M., will be devoted to a discussion on Press Design and Performance; the afternoon session, from 2.0 to 4.0 P.M., to a discussion on Hydraulic Control Systems.

The paper by Mr. Elkan and Mr. Lewis is published in this issue of the *Journal*.

INSTITUTE OF METALS

Award of Medals

The Council of the Institute of Metals has announced the following awards of medals:

The Institute of Metals Medal in Platinum for 1956 to Professor **Georges Leon Chaudron,** Doct. ès Sc., Membre de l'Académie des Sciences (Professor à la Sorbonne; Directeur du Laboratoire de Vitry du Centre National de la Recherche Scientifique) in recognition of his outstanding contributions to knowledge, particularly in the field of the light metals, which have greatly benefited the metal industries.

The Rosenhain Medal for 1956 to Dr. **Donald McLean** (Principal Scientific Officer, Metallurgy Division, National Physical Laboratory, Teddington) in recognition of his outstanding contributions to knowledge in the field of physical metallurgy.

The W. H. A. Robertson Medal for 1955 and Premium of Fifty Guineas to Mr. **E. J. Thackwell** (The Northern Aluminium Co., Ltd., Rogerstone, Mon.) for a paper on "*The Choice and Construction of Monolithic Linings for Twin-bath Induction Furnaces for Melting Aluminium Alloys.*"

"ACTA METALLURGICA"

Acta Metallurgica is an international Journal for the Science of Metals. It was initiated in 1953 by the American Society for Metals in co-operation with many of the leading Metallurgical Societies and Institutions of the world: its purpose is to provide a medium for the publication of papers describing theoretical and experimental investigations that contribute to the understanding of the properties and behaviour of metals in terms of fundamental particles, forces, and energies. It is published every two months. The business management of *Acta Metallurgica* has recently been taken over by Pergamon Press Inc., of New York.

The Institute of Metals has agreed to forward subscriptions from members of The Iron and Steel Institute.

Members of The Iron and Steel Institute who are not at present subscribers to *Acta Metallurgica*, but who are interested, can obtain full particulars from the Secretary.

Members of The Chemical Society, The Iron and Steel Institute, or The Institute of Metals have the privilege of subscribing to *Acta Metallurgica* at the reduced rate of £3 6s. 0d. per annum, payable in the United Kingdom.

Owing to an error, notices for renewal of subscriptions to *Acta Metallurgica* have been sent by Pergamon Press Inc., New York, to subscribers in the United Kingdom.

Members who are already subscribers should renew their subscriptions for 1956 by sending a remittance of £3 6s. 0d. to the Secretary, The Institute of Metals, 4 Grosvenor Gardens, London, S.W.1, and should *not* remit to Pergamon Press Inc., New York. Cheques should be made payable to "The Institute of Metals—Acta Metallurgica Account."

AFFILIATED LOCAL SOCIETIES

Lincolnshire Iron and Steel Institute

The list of Officers for the Session 1955–56 is as follows:

President

Lt.-Com. G. W. WELLS, R.N. (Retd.), D.L., M.I.Mech.E.

Vice-Presidents

G. D. ELLIOT, O.B.E.	L. REEVE
J. L. GASKELL	J. D. TOWNSEND
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Hon. Treasurer G. P. ELSON

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J. DODD	E. R. S. WATKIN
W. GEARY	J. MCLAUCHLAN
T. P. LLOYD	A. C. WATKISS

Swansea and District Metallurgical Society

The Council of the Swansea and District Metallurgical Society have decided to make a presentation to Mr. **U. Williams** in recognition of his long service as Honorary Treasurer of the Society. Members are invited to send contributions, to a maximum of 10s. 0d., to Mr. L. A. S. Perrett, 'Morlais,' Court Sart, Briton Ferry, Neath.

NEWS OF SCIENCE AND INDUSTRY

Symposium on Extraction Metallurgy of Less Common Metals—London

A symposium to discuss recent developments in extraction metallurgy, with special reference to the less common metals, has been arranged by the Institution of Mining and Metallurgy for 22nd and 23rd March, 1956. Advance copies of papers will be available and attendance is not restricted to members of the Institution. Full details may be obtained from the Secretary of the Institution, 44 Portland Place, London, W.1.

Changes of Address

CHRISTMAS AND WALTERS LTD. have removed to 287 Streatham High Road, London, S.W.16 (Telephone: Pollards 6589).

LAVINO (LONDON) LTD. have removed to Garrard House, 31–45 Gresham Street, London, E.C.2 (Telephone: Monarch 6137).

The Research Technical Committee of the ROLL MAKERS' ASSOCIATION has removed to Beaufort House, Newhall Street, Birmingham 3 (Telephone: Central 6661–3).

DIARY

1st Feb.—INSTITUTION OF ENGINEERING INSPECTION, BIRMINGHAM—"*Shell Moulding*," by M. C. Dixon—Birmingham Exchange and Engineering Centre, 7.30 P.M.

1st Feb.—MANCHESTER METALLURGICAL SOCIETY—"*Cast Iron*," by I. C. H. Hughes—Lecture Room, Central Library, Manchester, 6.30 P.M.

2nd Feb.—LEEDS METALLURGICAL SOCIETY—"*Metallurgy in Relation to Gas Turbines*," by G. M. Robertson—Large Chemistry Lecture Theatre, University of Leeds, 7.15 P.M.

6th Feb.—CLEVELAND INSTITUTION OF ENGINEERS—"*The Use of Refractories in the Iron and Steel Industry*," by Dr. H. R. Lahr—Cleveland Scientific and Technical Institution, Corporation Road, Middlesbrough, 6.30 P.M.

6th Feb.—THE SOCIETY OF ENGINEERS—Presidential Address, by Capt. H. F. Jackson—The Geological Society, Burlington House, London, 5.00 for 5.30 P.M.

7th Feb.—SHEFFIELD METALLURGICAL ASSOCIATION—"*Chromatographic Methods of Inorganic Analysis*," by A. A. North—B.I.S.R.A. Laboratories, Hoyle Street, Sheffield 3, 7.00 P.M.

7th–8th Feb.—BRITISH WELDING RESEARCH ASSOCIATION—"*Welding for Increased Productivity*"—Nottingham Mechanics' Institution, Trinity Square, Nottingham, 9.30 A.M. each day.

9th Feb.—EBBW VALE METALLURGICAL SOCIETY—"*Fuel Oil and its Application to the Steel Industry*," by V. H. Miller (Joint Meeting with Engineers Group).

9th Feb.—LIVERPOOL METALLURGICAL SOCIETY—"*Radiation Damage to Metals*," by W. M. Lomer—The Liverpool Engineering Society, The Temple, Dale Street, Liverpool, 7.00 P.M.

11th Feb.—SWANSEA AND DISTRICT METALLURGICAL SOCIETY—General Meeting, "*Some Metallurgical Aspects in the Design, Fabrication, and Maintenance of Engineering Structures*," by A. J. K. Honeyman—Central Library, Swansea, 6.30 P.M.

13th Feb.—NORTH EAST METALLURGICAL SOCIETY—"*An Outline of the Modern Metallurgy of Cast Iron*," by H. Morrogh—Cleveland Scientific and Technical Institution, Middlesbrough, 7.15 P.M.

14th Feb.—SHEFFIELD METALLURGICAL ASSOCIATION—"*Research and Development on Continuous Casting of Steel*," by J. Savage—B.I.S.R.A. Laboratories, Hoyle Street, Sheffield 3, 7.00 P.M.

14th Feb.—NEWPORT AND DISTRICT METALLURGICAL SOCIETY—"*The Continuous Casting of Steel*," by J. S. Morton—Whitehead Institute, Cardiff Road, Newport, 7.00 P.M.

14th Feb.—EAST MIDLANDS METALLURGICAL SOCIETY—Members' Night. Short papers given by Society Members—Nottingham and District Technical College, Shakespeare Street, Nottingham, 7.30 P.M.

15th Feb.—MANCHESTER METALLURGICAL SOCIETY—"*The Mechanical Properties of Metals*," by Professor R. W. K. Honeycombe—Lecture Room, Central Library, Manchester, 6.30 P.M.

15th Feb.—NORTH WALES METALLURGICAL SOCIETY—"*Recent Researches on the Deep Drawing Properties of Mild Steel Sheets*," by B. B. Hundy—County Primary School, Plymouth Street, Shotton, Nr. Chester, 7.15 P.M.

17th Feb.—WEST OF SCOTLAND IRON AND STEEL INSTITUTE—"*Electric Drives for Hot Rolling Mills*," by H. S. Brown and J. C. Easton (Joint meeting with The Iron and Steel Institute Engineers Group)—39 Elmbank Crescent, Glasgow, 6.45 P.M.

19th–23rd Feb.—AMERICAN INSTITUTE OF MINING AND METALLURGICAL ENGINEERS—Annual Meeting—New York.

20th Feb.—INSTITUTION OF ENGINEERING INSPECTION, WOLVERHAMPTON—"*Quality Control and Inspection of Steel Castings*," by R. B. Naylor—Compton Grange, Compton Road, Wolverhampton, 7.30 P.M.

20th Feb.—SHEFFIELD SOCIETY OF ENGINEERS AND METALLURGISTS—"*Aluminium in Engineering*," by E. G. West (Joint meeting with The Institute of Metals and The Sheffield Metallurgical Association)—The University Building, St. George's Square, Sheffield, 7.30 P.M.

20th Feb.—SHEFFIELD METALLURGICAL ASSOCIATION—"*Aluminium in Engineering*," by E. G. West (Joint meeting with the Society of Engineers and Metallurgists, and The Institute of Metals)—Engineering Lecture Theatre, St. George's Square, Sheffield, 7.30 P.M.

21st Feb.—SHEFFIELD METALLURGICAL ASSOCIATION—"*High Frequency Linings*," by K. W. Slack—B.I.S.R.A. Laboratories, Hoyle Street, Sheffield 3, 7.00 P.M.

22nd Feb.—SOCIETY OF CHEMICAL INDUSTRY—"*Control of Corrosion in the Dairying Industry*," by G. H. Botham and E. L. Crossley (Joint meeting with Food Group)—Chemical Society, Burlington House, Piccadilly, W.1, 6.30 P.M.

22nd Feb.—SHEFFIELD SOCIETY OF ENGINEERS AND METALLURGISTS—"*Fluid Lubrication in Wire Drawing*," by D. G. Christopherson, and "*Mechanics of Wire Drawing*," by J. G. Wistreich (Joint meeting with the Institution of Mechanical Engineers)—The University Building, St. George's Square, Sheffield, 6.30 P.M.

23rd Feb.—THE INSTITUTE OF FUEL, MIDLAND SECTION—"*Hot-Blast Cupola Furnaces*," by W. J. Driscoll—James Watt Institute, Great Charles Street, Birmingham 3, 6.00 P.M.

24th Feb.—INSTITUTE OF METALS—Informal Discussion on "*Furnace Refractories for the Use of the Non-ferrous Metal Industries*,"—The University, Edgbaston, Birmingham 15, 10.30 A.M.

28th Feb.—INSTITUTION OF PRODUCTION ENGINEERS—"*Shell Moulding*," by A. Cameron—Cleveland Scientific and Technical Institution, 7.00 P.M.

28th Feb.—SHEFFIELD METALLURGICAL ASSOCIATION—"*Gas Carburizing*," by F. W. Haywood—B.I.S.R.A. Laboratories, Hoyle Street, Sheffield 3, 7.00 P.M.

29th Feb.—MANCHESTER METALLURGICAL SOCIETY—"*B.I.S.R.A. Research on* (1) *Electric Arc Furnaces*; (2) *Ingots*," by W. H. Glaisher—Lecture Room, Central Library, Manchester, 6.30 P.M.

TRANSLATION SERVICE

(The previous announcement was made in the January, 1956, issue of the *Journal*, p. 96.)

TRANSLATION AVAILABLE

No. 507 (German). H. POHL: "Sizing of Lump Ores." (*Stahl u. Eisen*, 1955, vol. 75, 6th Oct., pp. 1295–1300).

TRANSLATION IN COURSE OF PREPARATION

(Italian). V. FERRARI and D. BELLINI: "Horizontal Pouring." (*Met. Ital.*, 1954, vol. 46, Nov., pp. 417–420).

ABSTRACTS OF CURRENT LITERATURE AND BOOK NOTICES

Iron and Steel Manufacture and Related Subjects

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MINERAL RESOURCES

Investigation of the Littleton Ridge Manganese Deposit and Vicinity, Southern District, Aroostook County, Maine. N. A. Eilertsen. (*U.S. Bur. Mines Rep. Invest.*, 1955, Feb., No. 5104). The deposit consisted of beds of impure manganiferous carbonate interbedded with green slate. Maximum manganese content was 13%.—B. G. B.

The Quebec-Labrador Iron Ore Development. J. Ritchie. (*Secretary*, 1955, **52**, Apr., 178–182). A short account of this scheme describes the development of the area and financial arrangements.—B. G. B.

ORES—MINING AND TREATMENT

Railway Wagons for Iron Ore and Other Bulk Materials—Optimum Sizes for British Railways. H. R. Mills. (*Engineering*, 1955, **179**, Mar. 18, 347–350). The author lists the criteria for bulk commodity wagons, compares American and European practice and puts forward definite recommendations for optimum sizes and types of wagons for British Railways. This article is based on extensive investigations carried out at B.I.S.R.A.—M. D. J. B.

New Ore Bridges at Ohio Works of U.S. Steel. (*Iron Steel Eng.*, 1955, **32**, Apr., 70–74). This article describes the replacement of two old ore bridges with two new units equipped with 22½-ton grabs. Adjustable voltage electrical control is used to obtain the 1640-tons/h grabbing capacity which was established as the operational requirement.—M. D. J. B.

The Use of Cyclones as Classifiers in the Wet Dressing Plant at Calbecht. K. Neumann. (*Z. Erz. u. Met.*, 1954, **7**, Dec., 545–546). A new plant for treating slimes of Salzgitter ore by high intensity magnetic separation uses cyclones for preparatory elimination of material below 20 and above 200 μ. Experimental data and the proposed flow scheme are reported in brief.—E. C.

The Cyclone Washer in the Wet Dressing Plant at Calbrecht. K. Bücken. (*Z. Erz. u. Met.*, 1954, **7**, Dec., 548–549). A brief account is given of the use of cyclones for treating the 15–3-mm fraction of Salzgitter ore. A mixed magnetite–ferro silicon medium is used to produce a concentrate with 35–37% Fe from a feed at 25·5% Fe. Angles of cone in relation to specific gravity of cut are discussed.—E. C.

An Agglomeration Process for Iron Ore Concentrates. W. F. Stowasser. (*Iron Steel Eng.*, 1955, **32**, Mar., 112–115). This paper describes a pilot plant built to convert iron ore concentrates into a form easily transported and smelted in blast-furnaces. The process developed in the pilot plant at Carrollville, Wisc., consists of balling the concentrates and burning these balls on a continuous horizontal grate.

Ore Dressing Research in India—1954. (*Chem. Process Eng.*, 1955, **36**, June, 215–216). The research is characterized by studies on the beneficiation of pyrites; concentration of cassiterite ore by jigging and tabling; beneficiation of ferruginous manganese ore by magnetic separation and froth flotation; flotation of alumina, and work on vermiculite and sands.—L. E. W.

The Use of Sintered Pyrrhotite Residues in the Production of Low Phosphorus Pig Iron. L. A. Miller. (*Blast Furn. Steel Plant*, 1955, **43**, July, 761–764, 794–795). The use of these residues, for the manufacture of sinter for blast furnaces is discussed. The raw material for sintering contains 64·0% Fe and 10% S, and the sinter 68·8% Fe and 0·05% S: sulphur in the raw material acts as fuel for sintering. The trials have been carried out on a 7-ft dia. blast-furnace.

Pelletizing of Iron Ore Concentrates. T. L. Joseph. (*Blast Furn. Steel Plant*, 1955, **43**, June, 641–646; July ,745–752). The production of pellets from finely powdered iron ore is considered in detail, with particular reference to U.S. experience. Work done outside the U.S.A. is also reviewed and the mechanism of the bonding of hematite and magnetite grains in pellets is discussed.—B. G. B.

Size Reduction. R. V. Riley. (*Chem. Process Eng.*, 1955, **36**, June, 199–202, 236). After reviewing scientific studies of crushing and grinding problems the author describes new machines and techniques and deals with constructional materials.—L. E. W.

On the Trial Manufacturing of Fluidized Roaster for Reduction. T. Mitsuhashi, M. Ueno, and M. Tanaka. (*J. Mech. Lab.*, 1955, **9**, Mar., 76–79). Fluidized roasting was tried for the magnetizing treatment of iron ore by charcoal gas. The experimental roaster is illustrated by photographs and a schematic diagram.—J. G. W.

Hardening Ore Pellets. B. Sewerynski and T. Wlazinska. (*Prace Inst. Ministerstwa Hutnictwa*, 1955, **7**, (1), 30–34). [In Polish]. Methods of firing ore pellets, processes taking place during firing, and the structure of fired pellets are described.

Pelletizing Process. B. Sewerynski and T. Wlazinska. (*Prace Inst. Ministerstwa Hutnictwa*, 1955, **7**, (1), 24–29). [In Polish]. The development of the ore pelletizing process is outlined. Results of pelletizing hematite ores, pyrite cinders, and blast-furnace flue dust are given.—V. G.

Agglomeration of Iron Ores. Z. Krotkiewski and B. Sewerynski. (*Prace Inst. Ministerstwa Hutnictwa*, 1955, **7**, (2–4), 101–105). [In Polish]. A review is given of the work on agglomeration of iron ores (sintering, briquetting, and pelletizing) carried out by the Polish Institutes of Metallurgy and Non-Ferrous Metals—V. G.

The Impactor Crushing Mill. A. Torlach. *Z.V.d.I.*, 1955, **98**, May 15, 493–496).

FUEL—PREPARATION, PROPERTIES, AND USES

The Assessment of Fuels with High Ballast Contents. E. Schwarz-Bergkampf. (*Berg. Hüttenmann. Monatsh.*, 1955, **100**, Jan., 57–59). A nomographic method of assessment is presented for determining net calorific values with allowance for transport costs as well as water and ash contents.

Applications of High Velocity Combustion to the Steel Industry. L. C. Peskin. (*Iron Steel Eng.*, 1955, **32**, Apr., 89–97). The use of high-velocity combustion technique for the production of high heat release is discussed. The creation and heat transfer characteristics of high velocity combustion are described. Applications of high-velocity combustion to the steel industry are examined.—M. D. J. B.

Mass Transfer Theories of Fuel Bed Combustion. R. S. Silver and R. W. Mackay. (*Brit. J. Appl. Phys.*, 1955, **6**, Aug., 267–271). An attempt is made to clarify the general position reached as a result of previously published work with special reference to combustion of carbon in solid fuel beds, and to enumerate the points on which certain of the more recent approaches agree or differ theoretically. A method of evaluating a live fuel bed is suggested as a logical beginning for further progress.—E. E. W.

A Study of the Effect of Integration of all Departments on the Fuel Economy of Iron and Steel Manufacture. W. F. Cartwright and J. F. Davies. (*J. Inst. Fuel*, 1955, **28**, May, 206–217). The fuel usage and costs in (*a*) a hypothetical integrated plant, and (*b*) a similar plant of 5 separate works are considered in detail, and compared. On a B.t.u. basis the integrated plant shows a saving of only 6·6%, but the advantage of integration is seen to be considerably greater if the basis of comparison is the commercial value of the heat used in the iron and steelmaking processes.—D. L. C. P.

The Thermal Ratio of Heat Exchangers, Taking into Account Longitudinal Heat Conduction. H. W. Hahnemann. (*Forsch. Ingenieurwesens*, 1953, **19**, (3), 31–87; (4), 105–114). It is shown that the performance of regenerative heat exchangers can be calculated by assuming steady flow conditions and using mean values, provided that the longitudinal heat conduction in the matrix is taken into account.—H. R. M.

Calculation of Heat Transmission in Cross Flow Regenerators. H. Hoppe. (*Forsch. Ingenieurwesens*, 1954, **20**, (5), 150–155). It is shown how the differential equations developed for counterflow regenerators may be used to determine the thermal ratio of crossflow regenerators, using a graphical method.—H. R. M.

The Ultimate Structure of Coal. A. Whitaker. (*J. Inst. Fuel*, 1955, **28**, May, 218–223, 255). The development of knowledge of coal structure is outlined, and it is indicated how new techniques may contribute to better knowledge.

Petrography of Coal. E. Perez Blanco. (*Inst. Nac. Carbon Bol. Inf.*, 1955, **4**, Mar., 57–76). [In Spanish]. Different methods for the petrographical analysis of coals are reviewed, together with characteristics of the petrographical constituents. Application of these methods to the preparation and selection of coals and blends for the manufacture of coke are discussed.—P. S.

Coal Preparation Plant at Lynemouth Colliery. (*Engineer*, 1955, **199**, May 6, 624–626).

Coal Preparation. M. Pozzetto. (*Rev. Ind. Min.*, 1955, **36**, Apr. 1, 467–489).

Improved Plastometer for Studying Agglutinating Behaviour of Caking Coals. G. L. Barthauer. (*Analy. Chem.*, 1955, **27**, June, 969–971).

Possibilities of Improving the quality of Coke for Blast Furnaces by rational Preparation of Coal Charges. F. Byrtus. (*Prace Inst. Ministerstwa Hutnictwa*, 1955, **7**, (2–4), 91–100). [In Polish]. After analysis of recent experiments carried out in Poland on the improvement of the quality of blast-furnace coke it is stated that effective improvement of the physical properties of coke can be attained by an appropriate selection of size distribution of individual coal, constituents in a blend, and stamping of charges to obtain an optimum bulk density. The influence of moisture content of charges in relation to its effect on bulk density is also discussed.—V. G.

Coke Formation and Coal Blending. I. G. C. Dryden, M. Griffith and R. A. Kirby. (*Coke Gas*, 1955, **17**, July, 272–279). Recent work and new ideas on the scientific basis of coke formation and its application to coal blending are comprehensively examined and discussed.—T. E. D.

Rebuilding of a Battery of 20 Silica Ovens at Derwenthaugh. R. Chambers. (*Yearbook Coke Oven Man. Assoc.*, 1955, 355–359).

Instrumentation at a Coke Oven Plant. J. J. Bailey. (*Yearbook Coke Oven Man. Assoc.*, 1955, 360–373). The importance of instrumentation is explained. Types of instruments are recommended. Collecting main recorder controllers, stack draught recorder controllers, and types of temperature measuring devices are discussed. Instruments and controllers in all stages of by-product recovery are also listed.—T. E. D.

Effects of Variations in Bulk Density on Coking Time. J. B. Gayle and E. L. Gulledge. (*U.S. Bur. Mines Rep. Inst.*, 1954, Jan., No. 5027). The results of carbonization tests carried out in a Tuscaloosa oven on 32 charges at a flue temperature of 2400° F are discussed. The bulk densities of the charge were varied by water or oil additions. A straight-line relationship between coking time and bulk density was developed—B. G. B.

The Effluent Disposal Problem. D. Hicks and J. W. Hunt. (*Yearbook Coke Oven Man. Assoc.*, 1955, 374–409). Aspects of the law relating to river pollution are explained and effluent standards are listed. The nature of coke-oven effluents is given, and present methods of disposal are surveyed. Alternative methods suggested include the use of deep bore holes, a pipeline to the sea, absorption processes, and chemical or biological treatment.—T. E. D.

Preliminary Notes on the Definition of Foundry Coke. E. Ferrara and I. Meltzer. (*Fonderia Ital.*, 1955, **4**, June, 296–300). [In Italian]. The authors discuss the fundamental requirements of foundry coke with particular reference to the physico-chemical properties of coke and its combustion characteristics.—M. D. J. B.

The Effect of the Physical and Chemical Properties of Coke on Its Combustion. R. A. Mott. (*Brit. Coke Res. Assoc.*, Sixth Conference, 1953, Oct. 28, 16–23). The effects of coke size, porosity of lumps, voidage in the bed, and gas resistance of irregular particles are assessed quantitatively. The significance of the shatter test is discussed. These factors are related to practice.—T. E. D.

Combustion and Reactivity of Carbon. W. F. K. Wynne-Jones. (*Brit. Coke Res. Assoc.*, Sixth Conference, 1953, Oct. 28, 8–15). The critical air blast test for carbon reactivity, and the effects of impurities on ignition temperature, are mentioned. The mechanism of carbon combustion is explained, and the different behaviour of various forms of carbon is discussed. Methods of studying reaction rates are given, with some results.—T. E. D.

Water-Gas Generators. L. Coron. (*Chaleur et Ind.*, 1955, **36**, June, 183–196). Influence of temperature on water-gas composition is mentioned, and its production in externally heated retorts is described. Methods of improving the thermal efficiency of the process are discussed. A scheme for the production of blue-water-gas with back-run is given, and carburetted water-gas systems are also given. The properties of various products of gas generators are compared.—T. E. D.

Two-Stage Gas Producer Plants: Method of Operation and Performance. (*Brit. Steelmaker*, 1955, **21**, Apr., 122–123, 125). The design and performance of the first two G.I two-stage pre-distillation gas-producer units installed in this country, early in 1954, are reviewed. The same basic generator can produce cold clean gas, hot raw gas, or hot de-tarred gas, and all three systems are considered. An indication is given of operating costs.—G. F.

An Automatic Voltage Control System for Electrical Precipitators. H. J. Hall. (*Trans. Amer. Inst. Elect. Eng.*, 1954, **73**, Part I, 124–127). A new system is described of automatic voltage control for electrostatic precipitation, based on the optimum precipitator sparking rate.—L. D. H.

The Venturi Washer for Blast Furnace Gas. J. E. Eberhardt and H. S. Graham. (*Iron Steel Eng.*, 1955, **32**, Mar., 66–71). This paper gives results obtained with a pilot plant Venturi gas washer with a nominal capacity of 25,000 ft^3/min

of gas, and a plant-scale Venturi gas washer with a capacity of 85,000 ft³/min of gas. Results indicate that the dirt content of the cleaned gas is dependent on the pressure loss across the Venturi section.—M. D. J. B.

The Process of Electro-Precipitation. D. O. Heinrich. (*Elect. Times*, 1955, **127**, June 16, 967–971; **128**, July 21, 80–82). The design, application, and the physical theory of precipitators used for particle extraction are discussed.

The Eastern Gas Grid. (*Usine Nouvelle*, Special No., Spring, 1955, 71–79). The gas pipeline system connecting the Saar with Eastern France is outlined. Details of equipment for compression, distribution and purification are given. Future projects are mentioned.—T. E. D.

TEMPERATURE MEASUREMENT AND CONTROL

The Two-Colour Pyrometer. P. Rodicq and G. Maillot. (*Rev. Mét.*, 1955, **52**, June, 477–484). A description is given of a two-colour pyrometer designed for measuring the temperature in a basic Bessemer converter by sighting the instrument through one of the blowing nozzles. The results of full-scale trials are described.—C. E. D.

Temperature Measurement in an Open-Hearth Furnace. G. Boos and J. Willems. (*Stahl u. Eisen*, 1955, **75**, July 14, 900–906). The authors measured the temperatures in an O.H. furnace by a Degussa immersion thermocouple and Naeser's colour pyrometer 'Bioptix.' Bath and slag temperatures were measured with the thermocouple, whereas the temperature of the roof was determined with the Bioptix. Shortly before the furnace is tapped the temperature difference between bath and roof is small if there is only a thin slag cover on the bath. This temperature difference of the order of 10° C is within the limits of accuracy of the instruments. Simultaneous temperature measurements with both instruments show the difference between black-body and colour temperature; this difference may be used for an appraisal of the quality of the steel.—T. G.

A Small Pneumatic Pyrometer. A. M. Godridge, R. Jackson and G. G. Thurlow. (*J. Sci. Instruments*, 1955, **32**, July, 279–282). The design and characteristics of a small pneumatic pyrometer are described, calibrated for use in the range 200–1550° C, and with an estimated accuracy of ± 5° C.—L. D. H.

REFRACTORY MATERIALS

Investigations of Magnesite Deposits on the Northern Margin of the Greywacke Zone of Salzburg and their Implications concerning the Origin of Magnesites in the Eastern Alps. H. Leitmeier and W. Siegl. (*Berg-Hüttenmänn Monatsh.*, 1954, **99**, Nov., 201–208; Dec., 221–230). The petrological and mineralogical nature of a number of magnesite deposits is described and discussed in the light of work by other investigators. Genetical implications of textures and mineral relationships are considered and the conclusion is reached that all these deposits are purely sedimentary in origin and not hydrothermal metasomatic.—E. C.

The Genesis of Layered and Fibrous Magnesite Rocks. W. Siegl. (*Berg. Hüttenmänn. Monatsh.*, 1955, **100**, Jan., 79–84). Textural features of magnesite deposits are studied in detail and their bearing on the mode of formation of the rocks is discussed. A sedimentary origin is suggested.—E. C.

Examination of the Bentonite at Deuchendorf by Optical and Electron Microscopy. E. Neuwirth. (*Berg. Hüttenmänn. Monatsh.*, 1954, **99**, Dec., 235–236). Optical and electron microscopical data are reported showing that the deposit is a true bentonite. The bulk of the material is devitrified volcanic glass, montmorillonite being the main constituent.—E. C.

Should We Have Specifications for Refractories? A. H. B. Cross. (*Refract. J.* 1955, **31**, Apr., 148–155). Experience of classification and specification of firebricks, silica bricks and insulating bricks is noted, and the possibility and utility of adopting various criteria examined.—D. L. C. P.

Phase Relationships of Iron-Oxide-Containing Spinels. III. Further Investigations on the Systems Fe-Mg-O and Fe–Mg–Cr–O, and General Relationships in the System Fe–Mg–Cr–Al–O. D. Woodhouse and J. White. (*Trans. Brit. Ceram. Soc.*, 1955, **54**, June, 333–366). Work was conducted up to 1650° C, and phase diagrams are given and discussed. The long life of a magnesite-chrome all-basic roof is explained.—D. L. C. P.

Preparing Polished Surfaces for Microscopic Examination in Reflected Light. E. W. Roberts. (*Trans. Brit. Ceram. Soc.*, 1955, **54**, Mar., 120–136). The technique of polishing is discussed and a method of polishing ceramic materials with abrasive fixed in discs of dental acrylic resin is given. A simple epicyclic carrier unsuitable for lapping at low speeds is described.—D. L. C. P.

Kinetics of the Thermal Dehydration of Clays, and the Isothermal Decomposition of the Clay Minerals. P. Murray, J. White. (*Trans. Brit. Ceram. Soc.*, 1955, **54**, Mar., 137–187). Theory of the kinetic decomposition of clays is dealt with and results of isothermal decomposition tests on various clay minerals are given and discussed. It is shown that the isothermal decomposition of the clay minerals proceeds according to the first-order kinetic law enabling velocity constants to be evaluated at different temperatures. From the velocity constants the Arrhenius parameters were determined and found to be specific for the different types of clay mineral.

The Differentiation of Aluminous Materials by Contraction Tests. J. F. Hyslop. (*Trans. Brit. Ceram. Soc.*, 1955, **54**, Mar., 113–119). Results of contraction tests at various temperatures are given for: clay grog, calcined cyanite, calcined bauxite and calcined alumina; the same materials with additions of Na_2O, K_2O, MgO, CaO; six commercial refractories.—D. L. C. P.

Refractory Mixes and their Applications. H. Zahlbruckner. (*Radex Rundschau*, 1955, Mar., 400–405). A brief account is given of U.S. developments in the uses of chrome-magnesite plastics, castables, gunmixes, and ramming mixes, particularly in the construction of open-hearth furnace doors. Steps in the process of preparation and construction are outlined and some economic data from American practice are compared with figures for brick linings.—E. C.

Alumino-Silicate Refractories for Open-Hearths. Standardization of size—Physico-Chemical Characteristics. M. Savarre. (*Centre Doc. Sidér., Circ. Inform. Tech.*, 1955, (7), 1425–1432). The advantages of standardizing refractory specifications are discussed and the adoption of the German system is suggested. Requirements for alumino-silicate structural refractories and casting refractories are mentioned, including composition, properties and dimensions.—T. E. D.

Survey of Information on the Problem of Steel Casting Bricks. L. Halm. (*Centre Doc. Sidér., Circ. Inform. Tech.*, 1955, (7), 1403–1424). The type of refractory bricks used and their performance is related to the type of steel produced in open hearth works and converter plants. Brick quality is discussed, and analyses and mechanical properties of ladle bricks are tabulated giving mean performance after 100 casts. The appearance of brick structure after service is reported, with particular reference to the effect of manganese.—T. E. D.

Corrosion and Erosion of Refractory Lining Bricks. (Laboratory Methods of Testing). P. Visseriat. (*Silicates Indust.*, 1955, **20**, May, 200–204). The modes of deterioration of lining bricks in use are discussed. Laboratory assessment of brick quality by means of small samples is shown to be inadequate, and tests utilizing whole bricks are proposed.—P. F.

Lining Crucibles for High-Frequency Induction Furnaces. A. P. Al'Ferov and N. K. Lekarev. (*Liteĭnoe Proizvodstvo*, 1955, (3), 31). [In Russian]. A fettling mixture and procedure for crucibles used in the H.F. induction melting of stainless and heat-resisting steels are described.—S. K.

Refractories in Great Britain—A Survey of Recent Trends. J. White. (*Refract. J.*, 1955, **31**, June, 342–370). Recent practice in the making and use of basic, silica, fireclay and carbon bricks is described.—D. L. C. P.

Refractories. (*Iron Age*, 1955, **175**, June, J2–J8). The development of refractory production in the U.S., present practice and likely trends are briefly discussed.—D. L. C. P.

BLAST-FURNACE PRACTICE AND PRODUCTION OF PIG IRON

Automatic Stock-Level Control on Blast-Furnaces. I. Kjellman and K. Grönblad. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 168–170). [This issue].

Account of a Journey to the United States. F. Clerf. (*Centre Doc. Sidér., Circ. Inform. Tech.*, 1955, (6), 1171–1200). Some aspects of a French blast-furnace and coke-oven productivity mission to the U.S.A. are discussed. Ore composition and methods of beneficiation are surveyed. Production of the various centres and transport costs are tabulated and compared. Blast-furnace operation is outlined and special features

of blast-furnace construction are illustrated. Ancillary equipment is also discussed.—T. E. D.

Great Lakes Steel Corporation Blows-In Largest Blast Furnace. (*Blast Furn. Steel Plant*, 1955, **43**, July, 765–766).

Blast-Furnace Construction. L. N. Misra. (*Tisco*, 1955, **2**, July, 147–153). The author discusses the construction of the various parts of the furnace and describes the charging and auxiliary equipment. The dismantling and rebuilding of a blast-furnace are outlined.—L. E. W.

Drive and Control of Blast-Furnace Turbo-Blowers. W. Runte. (*Stahl u. Eisen*, 1955, **75**, April 21, 461–474). Blast-furnace turbo-blowers and their drives should be laid out according to actual requirements for optimum economic performance. Turbo blowers supplying one furnace only should have speed control, whereas a number of axial blowers running at constant speed are best suited for supplying blast for several furnaces by a blast mains. The author discusses the various types of drives for blowers such as steam and gas turbines and electric motors and their advantages and shortcomings. The choice of the drive depends largely on local conditions, no general rule can therefore be given. The author concludes with the various types of control which are mainly governed by capital outlay.—T. G.

The Characteristics of Rotating-Piston Air-Blowers. A. D. Akimenko and A. A. Skvortsov. (*Liteĭnoe Proizvodstvo*, 1955, (1), 16–17). [In Russian]. The calculation of the characteristics of rotary blowers is considered with special reference to the two-impeller type.—S. K.

Using Steam-Enriched Blast in the Blast Furnace. F. Houdek. (*Hutník* (Prague), 1955, **5**, (7), 194–196). [In Czech]. An analysis of results of experiments with blasts at 630° C, containing 20 g of water per m^3, carried out over a period of several months on a works scale, showed a more uniform and efficient performance on adding steam, and a simultaneous reduction in the coke-rate.—P. F.

Experimental Production of Pig Iron and Steel From Cominco Iron Concentrates. B. G. Hunt, E. J. Kwasney, W. P. Campbell and S. L. Gertsman. (*Canad. Min. Met. Bull.*, 1955, **48**, May, 281–291). By-product iron sulphide, from lead and zinc ore separation, is roasted to produce H_2SO_4. The iron–zinc calcine was used in a 75-kW electric furnace to produce pig iron experimentally. Steel was produced from the pig iron. Results of these trials and quality of the steel produced are reported, including analyses and mechanical tests.

Experimental Granulation of Cast Iron and Pig Iron. R. Mitsche and E. M. Onitsch-Modl. (*Berg. Hüttenmänn. Monatsh.*, 1955, **100**, Mar., 121–126). A detailed account is given of investigations into possibilities of producing granules of a given size by some form of pre-granulation of the liquid metal stream. The tests cover pre-granulation by impact, screening, and by a multiple-groove trough; final cooling occurs in water in all cases. Only the last-named method proved reliable.—E. C.

Rebuilding 'D' Blast Furnace at Jamshedpur. L. N. Misra. (*Sci. Eng.*, 1954, **7**, Nov.-Dec., 111–115). The Tata Iron and Steel Co., has five blast-furnaces of which the 'A' furnace of Brassert design proved to be the best. In 1952 it became necessary to return the 'D' blast-furnace and a new 1000 ton furnace similar to 'A' furnace was built instead, the work being done by the works engineering department within the Steel Co. The new 'D' furnace has a hearth d. 22 ft 6 in., height, 94 ft, bosh angle 79° 42'; bosh dia. 26 ft 6 in.; neck 9 ft, large bell 14 ft; small bell 6 ft 6 in. and a total volume of 35,160 ft^3.—U. E.

Instrumentation of the Blast Furnace. R. V. Tamhankar and P. S. J. Rao. (*Tisco*, 1955, **2**, July, 120–134). The authors discuss principles involved in instrumentation and describe with circuits and diagrams the instrumentation and control of a blast-furnace. The controls available in the operator's cabin and their general lay-out are dealt with. (15 references).

Investigation of the Reduction Processes Taking Place in the Blast Furnace. J. Willems, W. Oelsen, and H. Genz. (*Stahl u. Eisen*, 1955, **75**, May 19, 618–624). The reduction processes taking place in the blast furnace were studied by charging two different burdens in the same furnace and the same burden in two furnaces of different design. The proportion in which reduction of the burden take place were expressed in Oelsen indices $K_{Mn,Si}$ and $K_{S,Si}$. Evaluation of these indices which are obtained by continuous recording gives a reliable guide for the selection of the best burden for a particular furnace. A number of examples are quoted.—T. G.

A Study on the Blast Furnace Operation. C. Sakai and Y. Imao. (*Sumitomo Metals*, 1955, **7**, Apr., 71–82). [In Japanese]. One month's record furnace operation is attributed to the use of a small number of high-grade raw materials, uniform ore mixing, high sinter burdens and increased tuyere size. Statistical analysis disclosed correlations between coke rate and blast temperature and top gas temperature, but none between solution loss and coke rate or CO/CO_2 ratio.—K. E. J.

Results of an Experimental Operation of a Blast Furnace with Iron Coke. H. Linde, K. Schwindt, and M. Paschke. (*Stahl u. Eisen*, 1955, **75**, June 2, 691–693). The authors report on the results obtained in a short-time (11 days) operation of a medium-size (291m^3 volume) blast furnace with iron coke only containing approximately 16% iron. Coke consumption when using iron coke was about 6% lower as compared with common metallurgical coke. Silicon reduction was higher for the same coke consumption per ton of pig iron.—T. G.

Experiments on the Reduction of Iron Ore. H. Schenck. (*Stahl u. Eisen*, 1955, **75**, June 2, 682–690). The author reports on preliminary experiments on the reduction behaviour of a number of iron ores in a stream of gas (CO and various mixtures of CO, CO_2, and N). The analysis of the reaction gases was continuously recorded. The main features in the reduction behaviour are porosity and gas permeability of the ores. Conventional as well as new methods were employed for determining porosity and gas permeability of the ores, sinter, and compacts. The following factors affect the reduction rate: Gas composition, gas velocity, temperature, density of the ore and its shrinkage under pressure and at progressing reduction. The experiments were carried out at temperatures up to 1100° C.—T. G.

Relation Between Mode of Operation of a Blast Furnace and the Quality of the Pig Iron and Their Effect on the Quality of Basic-Bessemer Steel Produced from it. W. Hummel, W. Loorz, and W. Oelsen. (*Stahl u. Eisen*, 1955, **75**, July 14, 885–900). The authors studied the mode of reduction in the blast furnace and introduce the indices $\Delta\%\ K_{Si,S}$ and $\Delta\%\ K_{Mn,Si}$ for the degree of reduction, similar to the Oelsen indices $K_{Si,S}$ and $K_{Mn,Si}$. The reduction depends largely on the ease of the gas flow and its distribution over the cross-section of the blast furnace. No clear relation between the qualities of the pig iron and the basic-Bessemer steel produced from it could be found, except for the nitrogen content of the steel. The effect of the temperature of the iron as well as that of the companion elements is largely eliminated by the equalizing effect of the mixer.—T. G.

Low-Shaft Smelting of Iron Ore. R. A. MacGregor. (*Trans. Min. Geol. Met. Inst. India*, 1952, **48**, July, 35–56).

The Application of Thermodynamics To The Control of the Iron Blast Furnace. K. T. Goodchild. (*J. Birm. Met. Soc.*, 1954, **34**, Sept., 87–111). The chemistry of the blast furnace and the thermodynamics of the iron making process are described. There is an extended discussion of free energy diagrams, and a method of blast furnace control based on temperature measurements in the stack of the furnace is proposed. (5 references).—P. M. C.

TREATMENT AND USES OF SLAG

The Improvement of the Service Life of Slag Ladles. E. Betting. (*Stahl u. Eisen*, 1955, **75**, July 14, 906–911). Multiaxial stresses acting between the hot bottom and the cooler upper part of slag ladles during filling are the main causes for their failure, but this cannot be avoided. The life of slag ladles, can however, be improved by: (i) reduction of stresses during travel by cushioned support, good order of the railway track, and careful marshalling; (ii) good cooling of the ladle mantle effected by crates instead of pots and by a corrugated shape of the mantle; (iii) using ladles of cast-steel instead of hematite iron.—T. G.

Utilisation of Cements containing Granulated and Crushed Slag in Surfacing Belgian Airfields and Roads. L. Blondiau. (*Silicates Indust.*, 1955, **20**, May, 205–210). A survey of experiences gained in the use of slag cements shows them to be equivalent to Portland cements in the applications considered.—P. F.

PRODUCTION OF STEEL

Layout and First Operation Data of the New Open-Hearth Steel Works II at Huckingen. K. G. Speith and H. Kobusch.

(*Stahl u. Eisen*, 1955, **75**, June 2, 654–699). An illustrated description of the newly built open-hearth steel works of Mannesmann Röhrenwerke at Huckingen. The plant consists of 150 ton Maerz furnaces, built by Demag Duisburg. All transport and charging is by overhead cranes. The furnaces are gas-fired and recently oil burners were installed. The output varies between 22,000 and 24,000 tons/month.

Temple Steelworks at St. Michel-de-Maurienne. (*Aciers Fins Spéc. Franç.*, 1955, July, 88–90).

Geneva Works, Columbia—Geneva Steel Division, United States Steel Corporation. C. Longenecker and H. E. Trout. (*Blast Furn. Steel Plant*, 1955, **43**, Aug. 869–908). A comprehensive account of this modern integrated steelworks in Utah which provides blooms, slabs, strip, sheet, plate and structural sections for the Western United States is given. Local iron ore is smelted in 3 blast-furnaces having a hearth dia. of 25 ft. There are 10 open-hearth furnaces, 7 tapping 248 tons and 3 tapping 270 tons. Medium and low carbon steels are produced: 60% is capped steel, 35% rimmed and 5% killed. Except for low alloy high tensile steels no alloy steels are produced. Steel is rolled by a 45-in. blooming and slabbing mill, a 132-in. strip and plate mill, a 32–26-in. structural mill and a temper mill, of which full details are given.

Great Lakes Steel Corp. T. J. Ess. (*Iron Steel Eng.*, 1955, **32**, Apr., GL2–GL27). This article gives a detailed description of the Great Lakes Steel Corp. Technical and operational information is given on raw material supplies, the coke oven plant, the blast-furnaces, the open hearth plant, the blooming and sheet mills, the merchant mills, the hot and cold strip mills and on all services. Tables of data on the blast-furnaces and mills are included.—M. D. J. B.

Iron and Steel. (*Iron Age*, 1955, **175**, June, D2–D21). Past, present and future trends in iron and steel production are reviewed. Modern steelworks processes are surveyed.

Mechanical Equipment and Organization for a Converter Steel Plant. J. Genot and H. Cabanes. (*Fonderia Ital.*, 1955, **4**, June, 301–302). [In Italian]. The authors describe a small modern French steel plant with special reference to stocking facilities, scrap preparation and furnace charging.

Production of Steel in a Converter with Oxygen Blast. S. A. Skomorokhov. (*Liteĭnoe Proizvodstvo*, 1955, (3), 21). [In Russian]. Recommended conditions for producing steel in an oxygen-blown converter are listed. Composition of the charge, blowing pressure, metal temperature and other factors are dealt with.—S. K.

Experiences in the Use of Oxygen in Basic Converter Practice. V. Chvátal. (*Hutník*, (Prague), 1955, **5**, (6), 162–165). [In Czech]. An analysis of technical and economic aspects of the method is made, with special reference to some experiments recently carried out by the author.—P. F.

Economic Aspects of The Oxygen Converter. W. C. Rueckel and J. W. Irvin. (*Iron Steel Eng.*, 1955, **32**, Mar., 61–63). The authors study the economies of oxygen blown converters and show that both capital costs and costs per ton are considerably lower than for comparable open hearth plant.

Pneumatic Steelmaking Processes. D. J. Carney. (*Blast Furn. Steel Plant*, 1955, **43**, June, 635, 640; July, 753–760). Pneumatic methods are defined as those in which air, oxygen or a combination of these and other oxidizing gases are blown through or over molten pig iron to make steel. The various types of processes are considered, particular reference being made to the Bessemer process and the control of nitrogen in the finished steel. Methods for detecting the end of the 'blow' are discussed.—B. G. B.

Temperatures of the Refractory Brickwork of a Small Acid Converter. W. Bading. (*Giesserei*, 1955, **42**, May 26, 288–290). An abridged article covering the work of several investigators, in which the type of converter investigated is illustrated. The thermal cycle for different parts of the lining is followed during the initial heating of the lining. Temperature changes at different places in the lining and of the shell are followed during the production use of the converter. These measurements suggested alterations to the design of the converter which resulted in an increased life. (8 references).—R. J. W.

Contribution to the Technology of the Production of Ball-Bearing Steels in the Basic Open-Hearth Furnace. F. Krumnikl. (*Hutník* (Prague), 1955, **5**, (6), 165–169). [In Czech]. Satisfactory results were obtained, comparable in every respect with those expected on using electric furnaces. Production technology, steel quality, and heat-treatments are discussed.

The Causes of Wasters in Steel Works and their Prevention. C. Brandau. (*Met. u. Giesserei Techn.*, 1951, **1**, May, 130–133). An attempt is made to list possible defects systematically under defects due to manufacture and defects due to the human factor. Faults of construction, scale models, material, and mould design are listed under the former, faults in casting, tapping, and cleaning under the latter.—L. J. L.

The Recuperative Steel-Melting Furnace. E. A. Nizkewitsch. (*Met. u. Giesserei Techn.*, 1951, **1**, Oct., 299–306; *Fuel Economy* (Moscow), 1951, **8**, Feb. 2). The author shows that, from the point of view of thermodynamics, the regenerative furnace is far superior to the open-hearth regenerator furnace. The installation of a circular steel radiation recuperator immediately behind the hearth, to ensure air preheating from 350° to 900° C, of a tubular steel corrector to ensure preheating to 350° C, and of a steam superheater is advocated.

Effect of Heat Regulation on Performance of Open-Hearth Furnaces. H. Müller and H. Stollberg. (*Met. u. Giesserei Techn.*, 1951, **1**, Aug., 248–252). Experiments have been carried out on different types of open hearth furnaces to determine the effect of heat regulation. Results agree well with British tests. To obtain favourable combustion conditions in the Maerz furnace there should be a collecting chamber for combustion air below the column of inflowing gases to ensure entrainment of air under the gas current.—L. J. L.

Influence of Temperature Evolution, during Refining on the Nitrogen Content of Basic Bessemer Steel. B. Trentini, P. Leroy, and M. Gombert. (*Rev. Mét.*, 1955, **52**, May, 418–427). By using a two-colour pyrometer in the base of a 19-ton basic Bessemer converter, it has been possible to compare the temperature curves corresponding to various additions. The variation in nitrogen content for a given final temperature could in part result from various thermal reactions which take place at the end of decarburization and of dephosphorization. The study of the effect on temperature of additions of scale, ore, or limestone is also assisted by this device.—G. E. D.

Control of the Basic-Bessemer Process Based on the Radiation of the Bath and the Spectrum of the Flame. F. Wever, W. Koch, H. Höfermann, B. A. Steinkopf, H. Knüppel, K. E. Mayer, and G. Wiethoff. (*Stahl u. Eisen*, 1955, **75**, May 5, 549–559). The radiation of the bath in a converter during blowing is nearly identical to that of the black-body, and so its temperature can be measured to a high accuracy photometrically. The spectrum of the flame varies during the reaction period. At the time of the drop of the carbon flame the continuous spectrum in the range 4000–4170 Å disappears, whilst during the dephosphorization period another continuous spectrum in the range 7025–7325 Å appears (from solid particles and fumes). The fumes reflect the reactions in the bath and their spectrum is therefore related to the processes in the converter. The intensity ratio of the two selected ranges is recorded by a spectrograph during blowing. At the end of blowing, the spectrum reflects Mn and P contents of the bath as well as temperature. Simultaneous spectrometer and temperature measurements make it possible to control the metallurgical reactions in the converter and to terminate blowing at the correct time when the phosphorus is burned off.—T. G.

Developments in Design of Modern Open Hearths. J. J. Seaver. (*Iron Steel Eng.*, 1955, **32**, June, 96–99).

Experimental Comparison of Steels Produced in the Acid and Basic Open-Hearth Furnaces. Z. Eminger and F. Kinský. (*Hutnické Listy*, 1955, **10**, (6), 329–346). [In Czech]. A detailed comparison is made of physical, mechanical, and other technologically important properties of a Cr–V steel used for the production of large crank-shafts.—P. F.

Ferro-Manganese Additions in Open Hearth Steelmaking. R. Tietig. (*Iron Steel Eng.*, 1955, **32**, Mar., 82–86). The author describes a method of controlled addition of manganese in the ladle accomplished by a mechanical feeder. It is claimed that the equipment can be amortized in six months from savings in manganese costs.—M. D. J. B.

Steel Quality. C. M. Parker. (*Steel Processing*, 1954, **40**, Dec., 781–785). The author emphasizes that quality must be judged with end-use in mind, and reviews the importance of adequate scrap and raw-material control.—P. M. C.

Open Hearth Operation and Control: The Use of Instrumentation. G. R. Bashforth. (*Brit. Steelmaker*, 1955, **21**, Mar., 80–86). The four main aspects of O.H. furnace control by instruments are the control of furnace pressure, combustion, regenerator temperature, and roof temperature. The author discusses the merits and use of the various instruments and techniques.—G. F.

Equipment for Open-Hearth Furnaces. P. Du Vivier de Streel. (*Arts et Manuf.*, 1955, June, 25–27). The equipment described includes burners for oil or natural gas, temperature regulators, reversing valves, and the use of oxygen.—B. G. B.

Activity Coefficient of Carbon in Alloys of Iron–Carbon–Silicon–Manganese. K. Sano, S. Murase, K. Okajima, and H. Abe. (*Nippon Kinzoku Gakkai-Si*, 1955, **19**, Jan., 35–38). [In Japanese].

The Activities of Coexisting Elements in Molten Iron (III), The Activity of Mn in Molten Fe–Mn Alloy. K. Sanbongi and M. Ohtani. (*Sci. Rep. Res. Inst. Tôhoku Univ.*, 1955, A, **7**, Apr., 204–209). The e.m.f. of the concentration cell + Fe–Mn/SiO_2–MnO–MgO–CaO/Mn − was measured at 1590 ± 5° C for various contents of Mn in iron. Relations between e.m.f. and activity of Mn, e.m.f. and temperature, were derived, and it is concluded that a Fe–Mn binary solution satisfies the requirements over the full range. Both this and the Fe–Ni and Fe–Co solutions are thought to obey Raoult's law.—J. G. W.

Processes of Steel Treatment Between Furnace and Casting Ladle. W. Küntscher and K. Steinheisser. (*Met. u. Giesserei Techn.*, 1951, **1**, Feb., 40–43). An account of "spout metallurgy" is given. It is claimed that a comparison between nitrogen contents of normal and "spout"-produced steels has shown that, in the latter, the nitrogen content is reduced. The experiments described show that it is possible to produce steels of superior electrical properties by treating the steel during flow from tap to ladle through a simple trough.—L. J. L.

Intensification of Steelmaking Processes. K. Radzwicki. (*Hutnik*, 1955, **22**, (5), 164–171). [In Polish]. The use of oxygen for the intensification of the O.H. process is outlined. The use of the air-blown convertor and L.D. process for steelmaking is described.—V. G.

Improving the Composition of Pig for Open-Hearth Furnaces in Mixers and Converters. F. Olszak and J. Natkaniec. (*Hutnik*, 1955, **22**, (5), 158–164). [In Polish]. Desulphurization of pig iron in ladles and mixers is described. Pre-refining in active mixers and its influence on the output of O.H. furnaces as well as the use of converters in co-operation with O.H. steelmaking are discussed.—V. G.

Dephosphorization of Alloys in Hydrogen. P. C. Ghosh and B. Chatterjee. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 153–155). [This issue].

Desiliconization of Pig Iron for Open-Hearth Furnaces. F. Olszak and A. Ofiok. (*Hutnik*, 1955, **22**, (5), 151–158). [In Polish]. The importance of desiliconization of pig to decrease the metallurgical load on O.H. furnaces is discussed. Methods used for desiliconization are described.—V. G.

Studies on the Decarburizing Reaction of Molten Fe–C Alloys. III. Y. Katsufuji and K. Niwa. (*Tetsu to Hagane*, 1955, **41**, Apr., 412–416). [In Japanese]. The rate of C removal in molten Fe–C alloys containing 0·2–4% of C was investigated, different known partial pressures of oxygen being used. The C removal rate increased with concentration at a given O_2 pressure, but the specific rate constant increased with C diminution and was independent of O_2 pressure. The activity coefficient of the reactants at the interface therefore appears to vary with the C concentration; N_2 gas admixed with the O_2 had no effect.—K. E. J.

The Improvement in Properties of Molten Iron. I. Blowing with Oxygen. I. Aoki and T. Tottori. (*Tetsu to Hagane*, 1955, **41**, Apr., 407–411). [In Japanese]. Oxygen was blown into molten iron. The C and Si contents of cast iron could be controlled, and its properties improved. The O_2 content was not increased, and impurity (Ti, Cr, and V) content decreased. Irons not normally suitable for ductile iron could be nodularized with Mg after oxygen blowing.—K. E. J.

A New Way to Control Arc Furnaces. C. W. Vokac. (*Iron Steel Eng.*, 1955, **32**, Feb., 76–81). The author describes the progress made in arc-furnace controls and discusses different methods of arc control, notably the balanced-beam contactor coupled to a balanced cable electrode drive and the air-counterbalanced hydraulic electrode drive.—M. D. J. B.

Recent Development of Electric Arc Furnaces for Steel Melting. T. Hayashi. (*Tetsu to Hagane*, 1955, **41**, May, 536–550). [In Japanese]. Developments in the U.S.A., Sweden, Germany, etc., are reviewed, and some recent Japanese operating results are analysed.—K. E. J.

The Oxidation Period in the Basic Arc Furnace. P. Fremunt and P. Pant. (*Slévárenství*, 1955, **3**, (7), 202–207). [In Czech]. Experimental results relating to the reactions occurring during the oxidation period are surveyed. The conclusions are that charging must be such as to lead to a 0·45% C content upon completion of melting.—P. F.

Induction Furnaces. P. P. Francis. (*Techn. Human.*, 1954–1955, **52**, (5), 229–254; (6), 266–278). Furnaces with and without magnetic cores are defined and described briefly. The theory of transformers is stated, and is applied to the case of a transformer functioning when it is short-circuited, as it does in an induction furnace. The theory is applied to induction furnaces with and without cores, and the power factor is studied. The paradoxical effects obtained in furnaces with or without magnetic cores when the frequency is altered are explained.—T. E. D.

High-Vacuum Furnaces. W. Scheibe. (*Z. Metallkunde*, 1955, **46**, Apr., 242–253). The construction of a number of modern types of high-vacuum furnaces is described and illustrated, and the possibilities of their application are discussed.—L. D. H.

The Superficial and Dynamic Activities of Sulphur in Iron–Carbon–Sulphur Liquid Alloys. P. Kozakevitch, S. Chatel, G. Urbain, and M. Sage. (*Rev. Mét.*, 1955, **52**, Feb., 139–160). It is possible to calculate the thermodynamic activity relative to sulphur by measurements of the superficial stress (γ) of liquid alloys Fe–C–S. This stress in industrial irons is primarily dependent on the thermodynamic activity of the sulphur contained. Using the γ measurements of the pure Fe–C–S alloys to calculate the thermodynamic activity of sulphur, this superficial stress may be assessed.—G. E. D.

Experiences in the Production of Heavy Steel Ingots. O. Bohuš. (*Hutnické Listy*, 1955, **10**, (8), 456–461). [In Czech]. Results of experiments on the effect of variations in the pouring rate, pouring temperature, aluminium additions, and other variables on steel quality and reduction of the reject rate are discussed primarily with reference to 100–130-ton ingots. The use of rotating moulds and of reducing atmospheres are advocated.—P. F.

Ingots and Ingot Production. I. Ingot Structure and Blowhole Formation. II. Ingot Defects. G. R. Bashforth. (*Brit. Steelmaker*, 1955, **21**, Apr., 116–119, 121; May, 146–150). In part I, the author first considers the mechanism of solidification of killed, semi-killed, and rimmed steels, and then discusses blowhole formation, illustrating the structure in various types of ingot. The iron-oxide–carbon reaction theory and the hydrogen-solution theory of blowhole formation are outlined. Careful control of all stages of the steelmaking process is stressed in the production of a round ingot. Part 2 deals first with ingot cracks, their probable causes being outlined and the importance of mould design being stressed. The other defects considered are hair-line cracks, piping, segregation, and non-metallic inclusions.—G. F.

Continuous Casting of Steel. (*Usine Nouvelle*, 1955, **11**, Jan. 27, 33–35). After a brief general introduction, continuous casting installations in the U.S.A., Germany, Austria, and the U.K. are mentioned. The only French plant is that of Aciéries et Forges de Loire at Unieux.—T. E. D.

Continuous Casting at Atlas Steels, Ltd. J. F. Black and F. W. Rys. (*Iron Steel Eng.*, 1955, **32**, June, 78–87). The commercial continuous casting machine recently put into operation at Atlas Steels Ltd. is described in detail.—M. D. J. B.

FOUNDRY PRACTICE

Casting Complicated Parts from Magnesium Cast Iron. V. I. Soldatenko, M. I. Rotenberg, and V. M. Yangunaev. (*Liteĭnoe Proizvodstvo*, 1955, (2), 5–6). [In Russian]. Deleterious effects of residual magnesium on the properties of magnesium-inoculated iron castings are discussed and an improved and tested inoculation technique is described in which the residual-magnesium content can be strictly regulated. The ladle is thoroughly de-slagged and placed in a chamber, where 0·5% of pure magnesium is added, the residual magnesium after the addition being 0·07–0·12%. Slag is removed from the metal surface and 10–18% of liquid cast iron and sufficient ferrosilicon to give a silicon content of 0·8–1% are simultaneously added.—S. K.

Device for Inoculating Cast Iron with an Alloy of Ferrosilicon and Magnesium. A. P. Ryl'nikov and A. I. Yakovlev. (*Liteĭnoe Proizvodstvo*, 1955, (3), 30–31). [In Russian]. A device successfully used for the safe inoculation of cast iron with magnesium alloys is described in which these alloys are introduced below the iron surface.—S. K.

Growth- and Heat-Stability of Silicon Iron with Spheroidal Graphite. I. E. Farafonov and Yu. G. Bobro. (*Liteĭnoe Proizvodstvo*, 1955, (3), 21–24). [In Russian]. The characteristics of high-silicon irons with spheroidal graphite were studied for the more common growth- and heat-resisting types. Three series of irons were used: irons containing 2·26–8·13% Si; irons with 3·61–6·29% Si, 1·73–3·54% Cr, and 17·55–20·54% Ni; high-silicon iron (21–33% Si). Irons inoculated with Mg and with various additions of Si were investigated. Microstructures of the irons are illustrated. Comparative data are presented for lamellar and spheroidal graphite irons. It is concluded that the properties of high-silicon iron with spheroidal graphite is as good as high-chromium and high-nickel irons.—S. K.

Mechanism of the Formation of Spheroidal Graphite. A. A. Gorshkov. (*Liteĭnoe Proizvodstvo*, 1955, (3), 17–20). [In Russian]. This is a contribution to the theory of the formation of spheroidal graphite through the action of inoculators and the related effect of "de-inoculation." After a discussion of existing theories the formation of graphite nuclei through the reaction of Mg vapour with the CO and CO_2 present in cast iron and the processes occurring in a bubble of the vapour rising through liquid iron are considered. The action of many elements in graphitization is discussed in relation to their position in the Periodic Table. Finally, the occurrence of spheroidal graphite in minerals is discussed.—S. K.

Suitability of T.I.S. Co. Pig Iron for the Manufacture of Spheroidal Graphite Cast Iron. D. R. Dhanbhoora and S. N. Anant Narayan. (*Tisco*, 1955, **2**, Apr., 59–75). The authors describe the techniques involved in the production of spheroidal graphite irons and then evaluate the engineering and service properties of these irons. The effect of chemical composition of T.I.S. Co. pig iron on spheroidal graphite formation and the suitability of such iron as a base material are discussed in some detail. Finally, the influence of current trends in cupola practice on the utilization of T.I.S. Co. pig iron is considered.—L. E. W.

Manganese-Titanium Pearlitic Malleable Iron. P. P. Berg and N. D. Titov. (*Liteĭnoe Proizvodstvo*, 1955, (3), 9–10). [In Russian]. An account is given of an investigation of the mechanical and casting properties, resistance to wear, and machinability of Mn–Ti pearlitic malleable cast irons with carefully controlled P and C contents. Dry and lubricated wear resistance tests on specimens, with spheroidized pearlite showed that Mn–Ti irons with 1·5–2·0% Mn are superior to ferritic malleable iron, inoculated anti-friction grey iron, and hardened steel under hard service conditions.—S. K.

The Mechanization of Ore-box Emptying in the Malleable Iron Foundry. K. Stölzel. (*Giessereitechnik*, 1955, **1**, May, 71–72).

How Users Benefit from Pearlitic Malleable Castings. G. B. Mannweiler. (*Iron Age*, 1955, **175**, May 19, 111–114). Properties of cast pearlitic alloy parts are noted and examples of applications given.—D. L. C. P.

Better Steel Castings for High-Temperature Plant. W. Siegfried and F. Eisermann. (*Metal Progress*, 1955, **67**, Apr., 100–101). Heat-treated mild-steel castings, with a low Cr and Mo content, used in high-pressure steam boilers and turbines, have been improved by addition of vanadium. Minor additions of Ni and Cu have reduced the tendency to notch sensitivity.—B. G. B.

High Quality Steel Castings. W. Schöning. (*Met. u. Giesserei Techn.*, 1951, **1**, Sept., 281).

Plain Steel Casting. A. Lincke. (*Met. u. Giesserei Techn.*, 1951, **1**, Nov., 336–338). A definition, in accordance with DIN 1681, of steel casting is given, and the effect of P and S contents on tensile strength, expansion, and impact strength is discussed.—L. J. L.

Steel Casting Alloys. A. Lincke. (*Met. u. Giesserei Techn.*, 1951, **1**, Nov., 331–336). The properties of cast steel alloys are discussed, and manufacturing processes are described.

Precision Alloy Steel Castings: Development of B.S.A. Process. (*Iron Steel*, 1955, **28**, Apr., 135–137). This article gives the advantages of the precision alloy-steel casting process, developed by B.S.A. Tools Ltd. originally for milling cutters and later applied to other articles. The structural and mechanical properties of the castings are briefly considered, and examples of applications and casting design are given.—G. F.

The Behaviour of Moulding Sand During the Moulding and Pouring of Grey Castings and the Testing of the Mould Sand Properties. W. Wegener. (*Giesserei*, 1955, **42**, May 12, 245–254; May 26, 280–285). The behaviour of moulding sand during the manufacture of the moulds is first described. A review of the methods of testing moulding sand, with particular reference to deformation properties, is given, and proposals are made for the determination of plasticity from the experimental results.—R. J. W.

Epoxy Patterns Give Easy Draws, Long Life. O. E. Fieroh. (*Foundry*, 1955, **83**, July, 178, 180, 182).

Synthetic Material in Pattern Construction. C. Rauh. (*Giesserei*, 1955, **42**, June 9, 310–312). Casting resins can be classified into two main groups: the polymerization resins and the polycondensation resins. The properties and possibilities of these are described; the treatment of the phenol casting resins is considered in detail and examples given.—R. J. W.

The Feeding Requirements of Nodular Iron Castings. R. C. Shnay. (*Canad. Metals*, 1955, **18**, June, 37–38). The design of riser required for nodular iron casting is different from that for normal grey-iron casting. Examples are given of the dimensions of risers for different shapes of casting which have been found suitable for nodular iron.—B. G. B.

Automated Moulding Line. R. H. Herrmann. (*Foundry*, 1955, **83**, June, 74–80). The production methods for the automated and semi-automated moulding lines in the grey-iron foundry of the Central Speciality Division of King-Seeley Corp., Ypsilanti, Mich., are described.—B. C. W.

Blown Shell Cores, Moulds Produced at High Rates. W. G. Patton. (*Iron Age*, 1955, **175**, June 16, 92–95). The 'Blobore' automatic shell machine is described which can turn out shell cores or moulds at up to 240 pieces per hour. Resin-coated sand is blown into electrically heated patterns in 1–5 s and curing is complete in 10–30 s.—D. L. C. P.

The Use of Carbon Dioxide Solidification Methods for Core Blowing Machines. W. Jansen. (*Giesserei*, 1955, **42**, Apr. 28, 236–237). The use of core-firing machines for the mass production of cores is discussed. High production from the machine enables appreciable reduction in core costs to be effected. Further cost reductions can be made using the CO_2 solidification method. A numerical comparison of costs shows a 10% saving by this latter method for a 1000-kg sand casting, over the normal oil-sand method.—R. J. W.

Classification and Use of Foundry Core Binders. R. Grochalski. (*Giessereitechnik*, 1955, **1**, Apr., 35–41).

Core Binder Problems in Foundries. Uhlitzsch. (*Met. u. Giesserei Techn.*, 1951, **1**, Mar., 82–84). General answers are given to the following questions: what does the founder expect of his cores; what does the founder expect of a core binder; and, what does the manufacturer of core binders expect of the founder?—L. J. L.

Casting Shaped Steel Parts in Metal Moulds. N. I. Domrachev. (*Liteĭnoe Proizvodstvo*, 1955, (3), 5–7). [In Russian]. Details are given of techniques successfully used at a Kirov works for casting relatively complex-shaped steel parts in metal moulds. Best results were obtained with steel moulds, but grey-iron moulds (3·2–3·6% C; 1·8–2·4% Si; 0·6–0·9% Mn; 0·1–0·2% P, and < 0·12% S), heat-treated by soaking for 2–3 h at 450–500° C, were also satisfactory. By using metal moulds production has been increased by a factor of 1·65 with existing equipment and working space, labour productivity has increased by a factor of 4–5 and great economies in mould and core mixtures have been effected.

Methods of Mechanization of Moulding Shops in Foundries. R. Chudzikiewicz. (*Przeglad Odlewnictwa*, 1955, **5**, (5), 137–141). [In Polish].

Elastic Chill Moulds for the Casting of Tensile Test Pieces. K. Wittmoser. (*Giesserei*, 1955, **42**, Apr. 28, 234–235). A two-page abstract of a Russian article which describes the casting of tensile test specimens using a cast-iron mould with an elastic insertion (lining). This surrounds the casting and imparts to it the desired properties. The stress/strain characteristics for the insert are illustrated and the technology for casting a 6-mm dia. tensile rod described.—R. J. W.

Cost Values of Investment Casting. B. G. MacKenzie. (*Canad. Metals*, 1955, **18**, Mar., 29–34).

Contribution to the Theory of Centrifugal Casting. L. S. Konstantinov. (*Liteĭnoe Proizvodstvo*, 1955, (1), 30–31). [In Russian]. A critical review is presented of some recent theoretical work on the determination of the free surface of a liquid in a vessel rotating about a horizontal axis.—S. K.

Foundry Specialises in Centrifugal Casting. E. Bremer. (*Foundry*, 1955, **83**, May, 130–135). A description is given of

the foundry practice used at the Sandusky Foundry and Machine Co., Sandusky, O., for the production of ferrous and non-ferrous centrifugal castings up to 60,000 lb in weight.

Cast Tooling. H. C. Marquardt. (*Precision Met. Mold.*, 1955, **13**, Apr., 38–39). The author discusses the economies produced by the use of investment-cast Stellite and tool-steel plastic moulding tools.—D. H.

Do Test Bars Give a True Picture of Properties of Investment Castings. N. J. Grant. (*Precision Met. Mold.*, 1955, **13**, Feb., 55–56, 87–88; Mar., 50–52, 68). Part I outlines the requirements of a test specimen and specific and general difficulties in producing investment cast test bars. These requirements and difficulties are illustrated in Part II by reference to specific case histories.—D. H.

Does Shell Moulding Belong in Your Foundry? J. G. Steinebach. (*Foundry*, 1955, **83**, June, 81–83). The factors to be taken into account by a foundry in considering whether to use shell moulds or shell cores are briefly discussed.—B. C. W.

Cast Bolts for Pipe Joints. C. K. Donoho. (*Metal Progress*, 1955, **67**, May, 86–88).

Colours in Foundries. J. Jemielewski. (*Przeglad Odlewnictwa*, 1955, **5**, (4), 107–109). [In Polish]. The influence of surroundings on the psychological state of the workers and the positive influence of the appropriate coloration of workshops is discussed.—V. G.

Accessories for Foundry Equipment. H. Voigt. (*Giesserei*, 1955, **42**, June 9, 321–322). The casting of a 6-cylinder crankcase with a water jacket is considered as an example of a difficult casting. Three separate proposals are made with a view to improved casting, particular reference being made to core treatment.—R. J. W.

Organic Foundry Design, Rationalization and Increase in Productivity. A. Bruckmayer. (*Giesserei*, 1955, **42**, May 26, 285–287). The increase in productivity in the foundry is first discussed in general terms. Rationalization (and subsequent increase in productivity) is first considered without capital outlay, and a numerical example given. The second stage of rationalization is the modernization and mechanization to increase the capacity of a given plant and implies the expenditure of capital. This is simply discussed in economic terms. The planned course of action must be elastically adaptable to the financial position.—R. J. W.

Rate of Cooling of Iron Castings and Core Heating. M. P. Samoïlov. (*Liteĭnoe Proizvodstvo*, 1955, (1), 22). [In Russian]. Temperature surveys on metal and core during the cooling of iron castings of various weights are reported, and the significance of the results in the consideration of gas pockets is discussed.—S. K.

Castability: Its Effect on Alloy Selection. W. A. Dubovick. (*Precision Met. Mold.*, 1955, **13**, June, 44–45, 48–50; July, 40–43, 69–71). Part I of this article lists the factors and conditions affecting castability and discusses the influence of each. In Part II specific alloys and alloy groups are described and the casting characteristics of each given.—D. H.

REHEATING FURNACES AND SOAKING PITS

Progress Review of Casting Bay and Soaking Pit Practice. L. H. W. Savage. (*Iron Steel*, 1955, **28**, Feb., 43–48; Mar., 98–100). The author discusses ingot heat conservation. The results obtained in surveys of works practice and in studies of works operations by B.I.S.R.A. are considered, and investigations of heat losses from solidifying ingots are summarized.—G. F.

Aspects of High Frequency Heating. R. R. Giles. (*Australian Inst. Metals: Australasian Eng.*, 1955, Jan. 7, 42–49). Two thermionic-value induction heaters of 45 and 25 kW rating are described. Equipment characteristics and their influence on the heat-treatment roles to be performed are discussed. A 3-kW dielectric heater with conveyor type oven is also described.—P. M. C.

The Fairless Soaking Pits. C. R. Wilt. (*Iron Steel Eng.*, 1955, **32**, Apr., 63–67). This article gives a detailed description of the 45-in. mill soaking pits at Fairless. The pits are two-way top-fired recuperative furnaces arranged in ten batteries of two furnaces each. Each furnace has a 140-ton ingot capacity.—M. D. J. B.

Rate of Heat Absorption of Steel. F. S. Bloom. (*Iron Steel Eng.*, 1955, **32**, May, 64–74). The author describes a method by which the heating rate of steel can be calculated and the centre temperature of the piece predicted. The method should make it possible to avoid wastage of money in building furnaces which will not meet requirements.—M. D. J. B.

Induction Heating of Ingots Proved Practical for Rolling. M. C. D. Hobbs. (*Iron Steel Eng.*, 1955, **32**, May, 123–129). The author discusses the reasons which led the Vancouver Rolling Mills to adopt induction heating for ingots on a production basis. The furnaces are dual-frequency twin-tube induction and are proving more economical to operate than rotary hearth, pusher, or batch furnaces.—M. D. J. B.

Mercury Arc Rectifiers for Frequency Changing on Induction Heating Equipment. (*Wild Barfield Heat Treatment J.*, 1955, **5**, June, 2–5). The article reviews the results of technical co-operation between G.W.B. Furnaces Ltd. and English Electric Co., Ltd., in the design and production of mercury-arc inverters to provide medium-frequency (1000–3000 cycles) supply for induction melting and heating equipment.—D. H.

High, Low, Dual—Which Frequency Do You Choose? (*Steel*, 1955, **136**, May 2, 126–130). Factors affecting the choice of frequency for induction heating are noted, and examples of installations given.—D. L. C. P.

HEAT-TREATMENT AND HEAT-TREATMENT FURNACES

Time–Temperature Diagrams as a Basis for the Heat-Treatment of Steels. F. Wever, A. Rose, and W. Strassburg. (*Forschungsberichte des Wirtschafts- und Verkehrsministeriums Nordrhein-Westfalen.* No. 75, 1954). A number of fundamental examples are used to demonstrate the use of isothermal time/temperature curves and of diagrams for continuous cooling. Heat-treatment processes, phase transformation, cooling, and hardening are explained by means of these diagrams.

Heat Treating. (*Iron Age*, 1955, **175**, June, C2–C14). Past, present, and future trends in heat-treating practice are reviewed. The increase in automatic handling is noted. Future operations will be characterized by increased precision.

Wide Steel Plate Continuously Heat Treated on New Integrated Line. W. D. Latiano. (*Iron Age*, 1955, **175**, May 5, 105–108). The continuous heat-treatment of wide steel plate on a new integrated furnace and quench press line at Lukens Steel Co. is described. The line handles plate 130 in. wide × 40 ft long.—D. L. C. P.

Controlled Atmosphere Furnaces. C. L. Ipsen. (*Metal Progress*, 1955, **67**, June, 91–95).

Case Hardening of Steels by Modified Gas Utilizing O_2. II. In the Case of Using CO_2. N. Shirai. (*Tetsu to Hagane*, 1955, **41**, Apr., 424–429). [In Japanese]. Irrespective of the type of steel, the carbonitriding power of the atmosphere was a maximum at a concentration of approx. 16 g of potassium ferrocyanide per litre of CO_2, which agreed well with calculations for the maximum concentration of C_2N_2O. The higher the heating temperature, the greater the case depth, but the lower the surface hardness. At high temperatures the maximum hardness was found slightly below the surface; structural reasons are suggested.—K. E. J.

The Application of Petrol for Gas Carburizing. A. Adachi and Y. Murakami. (*Tetsu to Hagane*, 1955, **41**, May, 520–524). [In Japanese]. Good carburizing gas was obtained by cracking light-oil gas with air at 900–980° C over a Ni catalyst, the best composition being H_2 40%, CO 20·5%, CO_2 0%, and CH_4 0·5%, with a dew-point of − 7° C. Carburetted petroleum gas could also be used. The carburizing power was varied by adjusting the dew-point and/or adding hydrocarbons.—K. E. J.

Salt Bath Furnace Performs Four Different Operations. E. W. Kerman. (*Iron Age*, 1955, **175**, May 19, 124–126). The article describes how a 65-kW salt-bath furnace installed for carburizing small parts is also used for hardening, brass brazing without hardening, and simultaneous brazing and carburizing.—D. L. C. P.

Reduction of Wear on Surfaces of Hardenable Iron Materials by Means of Flame-Hardening. K. Boeckhaus. (*Metalloberfläche*, 1955, **9**, May, B74–B78).

Improvement and Corrosion Protection of Surfaces by Means of High-Vacuum Vapour Treatment. W. Reichelt. (*Z. Metallkunde*, 1955, **46**, Apr., 268–271).

The Properties of Iron Carbonitrides. R. Bridelle and A. Michel. (*Rev. Mét.*, 1955, **52**, May, 397–400). The carbonitride phases to which iron gives rise are discussed. These phases are derived either from nitrides Fe_4N, Fe_3N, or Fe_2N,

or from the carbides Fe_3C or Fe_4C. The structural properties, conditions of formation, the Curie point, and thermal history are indicated in each case.—G. E. D.

Modern U.S. Gas Carburising and Carbonitriding Practice. N. K. Koebel. (*Metallurgia*, 1955, **52**, July, 3–9).

Steam Heat Treating Boosts Processing Efficiency. F. L. Spangler. (*Steel*, 1955, **136**, May 9, 82–84). The purpose and practice of steam heat-treating are described. Ferrous metals may be heated in steam to about 1000° F for ½ h to give a thin, tightly adherent oxide coating. The process has been used for scale-free tempering and stress relief; lengthening the service life of tools and cast iron parts; providing a blue surface finish; improving compressive strength and hardness of powdered iron parts; improving corrosion resistance; preparing surfaces for coating.—D. L. C. P.

On Scale Penetration in Steel at High Temperatures. M. Washida, D. Yamamoto and K. Yoshida. (*Tetsu to Hagane*, 1955, **41**, May, 515–519). [In Japanese]. Investigations are reported on the penetration of oxidation below the surface scale when rimming steel is heated in various oxidizing atmospheres. Light and dark blue-grey scale is found, the depth and scale size increasing with temperature; particularly massive scale is found > 1300° C, confirming that oxidation contributes to the defect of over-heating.—K. E. J.

On Bright Annealing of Steel in Converted Propane Gas. A. Adachi and S. Yamada. (*Nippon Kinzoku Gakkai-Si*, 1955, **19**, Jan., 1–4). [In Japanese]. Propane, reacted with air in presence of a Ni catalyst, can be used for bright annealing of plain C steel without surface discoloration. The effects of air/gas ratio and conversion temperature on the final gas composition are described. Low-C steel is annealed in gas containing CO_2 6, CO 12·8, CH_4 0·5 and H_2 12·8%, balance N_2, converted at 930°: for high-C steel the gas used contains CO_2 0, CO 23·4, CH_4 0·9 and H_2 31%, balance N_2.

Continuous Annealing. J. Soyring. (*Iron Steel Eng.*, 1955, **32**, Mar., 79–81). The author describes the continuous strip annealing line at Gary Sheet and Tin Mill which cleans, anneals and cools cold reduced strip. The line produces 30 tons/h of clean, bright and uniformly annealed strip of various widths and thicknesses.—M. D. J. B.

Single Stack Coil Annealing. W. F. Carter. (*Iron Steel Eng.*, 1955, **32**, Mar., 76–79). The author describes and discusses briefly the operating experiences of a strip mill with modern single-stack high convection radiant tube core annealing furnaces. Comparisons are made between the modern equipment and the previous installation at the mill.

Quality Comparison of Box *v.* Strip Annealing of Tin Mill Products. W. H. Swisshelm. (*Iron Steel Eng.*, 1955, **32**, Mar., 74–76). The author discusses radiant tube annealing and continuous annealing of tin mill products. It appears that both the as-annealed hardness and finished tin plate hardness of the continuous anneal material is approximately 3 points above the box anneal. The hardness uniformity of the strip anneal material is much superior to that of the box anneal.

Effect of Variation in Hardening and Tempering Temperatures. E. Gregory. (*Wild Barfield Heat Treatment J.*, 1955, **5**, Mar., 2–5). The author describes an investigation into the effects of variations in hardening and tempering temperatures on the precipitation of chromium, molybdenum and vanadium carbides and the results of shock and impact tests.—D. H.

Hardenability. A Simple Method of Determining Transverse Hardness Curves. H. M. Walter. (*Automobile Eng.*, 1955, **45**, May, 207–208). The method consists of cutting a disc of the steel under test, and by means of drilled centre holes and a long belt, clamping this test-piece between two discs of mild steel of the same diameter. All mating faces are ground before assembly of the composite bar. After hardening, the test disc is separated, surface ground, and hardness traverses carried out. The results are shown to be in good agreement with those obtained from solid bars of the same material.

Studies on the Quenching Media. VI. Cooling Ability of Concentrated Salt Solutions. M. Tagaya and I. Tamura. (*Nippon Kinzoku Gakkai-Si*, 1952, **16**, Dec., 652–655). [In Japanese]. Cooling curves of concentrated solutions of several salts were determined. Those in which the solubility decreases with rise in bath temperature were found to be satisfactory for general steel quenching, concentrated Na_2CO_3 solution being outstanding.—K. E. J.

Don't Neglect the Quench. D. F. Hammer. (*Steel Processing*, 1955, **41**, Jan., 39–46, 48). The elementary principles of quenching and its metallurgical effects are described with reference to the conventional **S**-curve or isothermal transformation diagram. Hardenability is then discussed, and results of end quench hardenability tests on a 1% C 1·5% Cr steel are used to demonstrate the efficiencies of different quenching media. The loss of quenching " power " of water when its temperature is raised, and the partial rectification of this trouble by the addition of salt is illustrated. The advantages of oil quenching, particularly its high temperature effectiveness and suitability for alloy steels, are discussed. (3 references).—P. M. C.

FORGING, STAMPING, DRAWING, AND PRESSING

Modern Forging Presses and Their Control. R. M. L. Elkan and J. T. Lewis. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 200–215). [This issue].

Installation of a 2000-ton Forging Press. (*Engineer*, 1955, **200**, July 15, 71–74).

Plant Extensions at a Steel Works at Rotherham. (*Engineer*, 1955, **200**, July 1, 13–15).

Computation of Stresses arising in Foundations of Power Driven Forge Hammers in Operation. A. Geleji and G. Dévényi. (*Acta Techn.*, 1955, **11**, (1–2), 217–229). [In German]. The paper is a further development of " Duration and Efficiency of the Thrust Process in Forging " published by the author (*Acta Techn.*, 1951, **1**, 299–318,)and deals with a new method of evaluating the stresses as a function of the design and mode of performance of the power hammers and their bases.—P. F.

Causes of Rejects and their Prevention in Drop-Forging. R. Hrivnak. (*Hutník*, (Prague), 1955, **5**, (6), 176–180). [In Czech].

Die Design Solves Small Part Production Problems. F. Strasser. (*Iron Age*, 1955, **175**, June 9, 78–80). Examples are given of methods of stamping small parts, particularly from thin metal and with closely spaced holes.—D. L. C. P.

Presswork. (*Iron Age*, 1955, **175**, June, 12–116). Past, present, and possible future trends in press design and pressing techniques are described.—D. L. C. P.

Combination of Machining and Press Operations. F. Strasser. (*Iron Steel*, 1955, **28**, Apr., 149–150). The author gives data dealing chiefly with components prepared by some machining process and later given a finishing press operation, the reverse process also being considered. The advantages of such combined operations are outlined.—G. F.

The Prevention of Stretcher Strains. H. P. Tardif. (*Steel Processing*, 1955, **41**, Apr., 241–244, 260, 263). The relation between the tendency of mild steel sheet to exhibit stretcher strains in a pressing operation and its yield point elongation in a tensile test is discussed. Methods for removing the troublesome yield point; e.g. by annealing in moist hydrogen, alloying and heat treatment, are reviewed. Such methods have proved of only limited use, and the normal way of curing the condition is by some form of pre-straining. The commonly employed processes of temper-rolling, and roller levelling are described, and the still troublesome phenomenon of the return of the yield point (ageing) after such processes is discussed. (30 references).—P. M. C.

The Behaviour of Sheetmetal in Presswork. E. Hennessy. (*Australasian Eng.*, 1955, Jan. 7, 64–66). Some of the problems arising in the production of modern pressed steel automobile wheels are discussed. The importance of strength and fatigue resistance are emphasized, and a method for testing the fatigue life of a wheel is described. The methods and operations of rim manufacture from straight strip, and of pressed centre discs are outlined, together with brief notes on the final assembly of disc and rim.—P. M. C.

Low Cost Production of Limited Quantities of Stampings. F. Strasser. (*Steel Processing*, 1955, **41**, Jan., 35–38). The importance of die costs when limited numbers of stamped components are to be made is emphasized, and special measures which may be employed to minimize such costs are discussed under the headings: subdivision of operations; construction of ' temporary ' or ' emergency ' dies; the use of universal dies; the use of inexpensive materials in place of steel, and special tooling such as the Guerin process.—P. M. C.

Friction in the Sheet Drawing. H. Yamanouchi and I. Hayashi. (*Waseda Univ., Rep. Castings Res. Lab.*, 1955, (6),

58–60). Die pressures and drawing forces in sheet drawing have been measured, and also the influences of die shape. For the same reduction in area, the larger the die angle the greater the pressure. For the same angle, the greater the reduction in area the less the total pressure. No direct relation exists between die pressure and coefficient of friction, but the latter decreases as die angle increases for the same pressure. For the same die angle, the coefficient of friction first decreases, then increases. Results are in accordance with the theory of E. Siebel.—K. E. J.

New Investigations on the Drawing and Entering of Steel Bars.—II. W. Lueg and K. H. Treptow. (*Stahl u. Eisen*, 1955, **75**, June 16, 769–776). The authors discuss the forces arising in drawing bars at various die angles and the effect of die angle on the mode of deformation. From theoretical considerations the mode of deformation over the cross-section was deduced and plotted. On this basis, trials were performed in order to measure drawing and compression stresses as a function of the diameter of the bars. The difference in deformation as a function of reduction in area and die angle was established by micrographs of the material after a recrystallization treatment by the difference in grain-size between the rim and the centre of the bars. Further tests were carried out to determine the degree of spring-back of the bars after drawing for various reductions, die angles and bearing lengths. It was found that the drawing forces increased with increasing bearing length. It was also found that spring-back is closely related to residual stresses so that it is possible to deduce the residual stresses in a bar from the degree of spring-back.—T. G.

Power Requirements and Forces Developed in the Cold Roll-Drawing of Tubes. A. Geleji. (*Acta Techn.*, 1955, **11**, (3–4), 461–478). [In German]. A simplified method of evaluating important parameters in roll-drawing, sufficiently accurate for all practical applications, is developed, and a graphical method for constructing the envelopes of the roller profiles is described.—P. F.

Spin-Forming. (*Aircraft Prod.*, 1955, **17**, Aug., 314–322). A power-operated and tracer-controlled stretch-flow forming lathe manufactured in Germany is described and illustrated. The method of production of hollow and tapered parts with accurately controlled thickness and varying profile is outlined.—T. E. D.

Tangential Stretch-Forming. (*Aircraft Prod.*, 1955, **17**, July, 290–292). Details are given of a stretch-forming press which allows direct curved draw. An attachment is provided for local redrawing operations.—T. E. D.

Study on the Internal Friction of Carbon Steel Wires in the Processes of Cold Drawing. T. Nishihara and H. Miki. (*Bull. Eng. Res. Inst. Kyoto Univ.*, 1954, **5**, Mar., 17–20). [In Japanese]. Logarithmic-decrement/frequency results were obtained for 0·6% and 0·1% C steel wires during successive stages of drawing. For high-C steel, the internal stress gradient reaches a maximum at approximately 40% total reduction; the value is approximately 60% for low-C steel. When reductions exceeding these figures have been made, subsequent reductions have a marked influence on the properties of the wires.

Studies on the Wire Drawing with Back Tension. I. On Pure Iron Wire. I. Gokyu. (*Nippon Kinzoku Gakkai-Si*, 1952, **16**, Dec., 674–679). [In Japanese]. The following advantages of using back tension were confirmed on iron wire: decrease of compressive and increase of tensile deformation; decrease of die pressure and friction; small temperature rise; high drawing speed; reduced power consumption; and improved and more uniform properties in the final wire.—K. E. J.

Studies on the Wire Drawing with Back Tension. II. On Carbon Steel Wire. I. I. Gokyu and T. Kitsukawa. (*Nippon Kingzoku Gakkai-Si*, 1952, **16**, Dec., 679–682). [In Japanese]. Tests with a patented 0·6% C steel wire confirmed the following advantages of the back-tension method: decrease of die pressure; decrease of friction and increase of deformation efficiency; and the smaller the number of passes, the higher the efficiency.—K. E. J.

Some Thoughts Upon Accident Prevention in the Steel Wire Industry. D. W. Bedford. (*Frederick Smith and Co., Wire Manufacturers Ltd.*, Mar., 30, 1955). Features of processes and equipment of a steel-wire mill, bearing a strong accident risk are pointed out and means of ensuring safety from accident are discussed.—J. G. W.

The Production of Patented Wire for Valve Springs. O. Page. (*Frederick Smith and Co., Wire Manufacturers Ltd.*, Mar. 30, 1955). The stages of manufacture of patented wire for valve springs are described with particular reference to the metallurgical controls required to attain the mechanical properties listed in B.S.S. 1408/EN.49.D. In discussing the air-patenting process equipment for forced-air quenching is described. Details are also quoted of suitable grit sizes for the removal of specified amounts of decarburized skin during the grinding operation. Particulars of mechanical tests on the finished wire are quoted, including results of fatigue tests on wire patented with and without an air jet, at various stages of manufacture.—J. G. W.

Torque Motor Drives for the Wire Industry. R. K. Larson. (*Wire and Wire Prod.*, 1955, **30**, Feb., 172–177, 217). Various types of low-cost electric reel drives for use in wire manufacture are discussed and numerous performance curves of torque motors are shown.—J. G. W.

Considerations for Selecting Steel Extrusions. S. O. Evans. (*Metal Progress*, 1955, **67**, Apr., 91–95). Factors affecting the economical production of extruded steel shapes of the Ugine-Séjournet process are discussed. The method permits the use of new alloys which are difficult or impossible to work by previous methods. Special shaped tubes can be made with outside and inside shapes that are disimilar. Solid shapes can be made which are impossible to make by rolling.—B. G. B.

Hot Extrusion of Stainless and Alloy Steel Tubing. (*Engineer*, 1955, **200**, July 1, 26–27). This article describes a plant designed specifically for the production of stainless steel tubing by the Ugine-Séjournet hot extrusion process. The method involves the use of molten glass as a lubricant.

Extruded Low Carbon Steels. R. L. Hugo. (*Product Eng.*, 1955, **26**, July, 129–131). The production of carbon steel extrusions is described, and numerous examples are given of their application in industry, both for simple and intricate sections.—E. A. C.

Extrusion Through Wedge Shaped Dies. I. W. Johnson. (*J. Mech. Phys. Solids*, 1955, **3**, Apr. 218–230). Results of numerical calculation of steady-state pressure are given for plane-strain, direct extrusion through square and wedge-shaped dies. The variation of extrusion pressure with coefficient of friction is presented graphically for a range of reductions and die angles. The calculations are based on the Hill-Tupper theory of sheet drawing and are applicable to extrusion in plane strain.—J. G. W.

Chip-free Cold and Hot Deformation. K. W. Michler. (*Metall*, 1955, **9**, Apr., 259–266). Modern methods of bolt and nut manufacture are described.—J. G. W.

The Calculation of Wire Drawing Force. (I) The Case of Conventional Wire Drawing with Work Hardening. K. Nakamura, H. Takahashi, and J. Kurihara. (*J. Mech. Lab.*, 1955, **9**, Jan., 36–41). A formula for the drawing force is derived on the basis of the Davis and Dokos analysis of plastic deformation for the case of a work-hardening metal, but corrected for additional deformation with large die angles, by the Körber–Eichinger method. The formula agreed satisfactorily with measurements on mild-steel and copper wires drawn with seed oil, in the range $r = 10$–24% and $\alpha = 5 \cdot 20°$ provided a coefficient of friction of 0·075 was assumed.

The Calculation of Wire Drawing Force. (II) The Case of Wire Drawing with Backpull. K. Nakamura, H. Takahashi, and S. Miyoshi. (*J. Mech. Lab.*, 1955, **9**, Jan., 42–46). The formula for drawing force developed in the first paper was extended to the case of drawing with backpull, and was tested experimentally. MacLellan's method of calculating the coefficient of friction was used and it was found necessary to adopt an empirical correction factor of about 0·9 for mild steel and 0·8 for copper wires.—J. G. W.

The Turks Head in Shaped Wire Production. L. J. Sarkozy. (*Wire and Wire Prod.*, 1955, **30**, Apr., 423–426; May, 548–551). The types and capacities of Turks heads are described with reference to the products of the Fenn Manufacturing Co., Conn. The second part contains descriptions of plant employing Turks heads, and some examples of manufacturing schedules for simple sections are quoted.—J. G. W.

The State of Lubrication in Wire-Drawing Operations. R. Tourret. (*Wire and Wire Prod.*, 1955, **30**, Mar., 299–303, 347). Results of laboratory tests with copper and aluminium wires and irradiated dies are discussed. The author concludes that the state of lubrication is boundary for most metals, with some admixture of hydrodynamic lubrication in the case of lead and aluminium.—J. G. W.

Graphic Analysis of the Relation between the Wire and Capstan Speeds on Multiple Wire-Drawing Machines. J. A. Giaro. (*Wire and Wire Prod.*, 1955, **30**, Mar., 305–312). A graphical method is presented for relating pass reductions and capstan speeds in slip-cone and non-slip storage multiple-hole wire-drawing machines.—J. G. W.

Accurate Measuring of Insulated and Bare Wire, Its Importance and Techniques. A. W. Nelson. (*Wire and Wire Prod.*, 1955, **30**, Mar., 313–314, 349–351). Continuous contact measurement of the length of insulated and bare wires is discussed. It is concluded that a 0·1% accuracy is attainable.

Mechanical Properties of Steel Wire Drawn by Back Tension Wire Drawing. T. Ueda and K. Asakura. (*Osaka Univ., Techn. Rep.*, 1954, **4**, Oct., 337–345). The mechanical properties of 0·62% C rope wire, air-patented and drawn 85·4% with and without backpull, at about 35 ft/min, were compared. The number of torsions increased, the tensile strength decreased slightly, hardness distribution became more uniform, and the fatigue limit increased with back pull up to an optimum ratio of 50% of back to front tensions.—J. G. W.

Lubricants for Wire Drawing. A. L. H. Perry. (*Sci. Lubrication*, 1955, **7**, May, 14–18). The processes and equipment used in wire drawing are described, and the functions of the lubricant (in preventing pick-up or breakage, reducing power required, cooling, improving finish, etc.) and its desirable properties, are discussed. In many cases, a state of mixed boundary and hydrodynamic lubrication exists. Details are given of the pre-treatments and lubricants employed in drawing wire of steel, copper, and aluminium and its alloys.—K. E. J.

Largest American-Built Extrusion Press for West Coast Operation. (*Steel Processing*, 1955, **41**, Jan., 19–21). A brief description is given of the 12,000-ton extrusion press being built by the Lombard Corporation for the U.S. Air Force as part of its Heavy Press Programme.—P. M. C.

Hot Extrusion at J. and L. Solid Carbon Steel Sections Now In Production. (*Steel Processing*, 1955, **41**, Apr., 230–233, 256). A new $1¼ million hot-extrusion plant is now in production at Jones and Laughlin Steel Corp., Pittsburgh. A 1000-ton Loewy Hydropress extrudes solid section- in low- and medium carbon steels by the Ugine–Sejournet process (using glass as the lubricant). The sections produced are those which are too complex or uneconomical for rolling.—P. M. C.

On Unsymmetrical Extrusion in Plane Strain. A. P. Green. (*J. Mech. Phys. Solids*, 1955, **3**, Apr., 189–196). Slip-line fields are proposed and extrusion pressures are calculated for extrusion through a square die situated either unsymmetrically at the end or in the side of a container. The mathematical analysis is supported by experiments with Plasticine.—J. G. W.

Extrusions Put Squeeze on Costs. R. M. Love. (*Steel*, 1955, **136**, May 9, 78–81). The advantages of the growing practice of using ferrous and non-ferrous extrusions in the manufacture of component parts are explained with examples, and some experiences of producers are noted.—D. L. C. P.

MACHINERY FOR IRON AND STEEL PLANT

Precision Metering System for Oxygen Flow under Constant Pressure. Boitel. (*Centre Doc. Sidér., Circ. Inform. Tech.*, 1955, **12**, (4), 801–804). After the principal flow meter, the conduit branches. One branch has a regulating valve, and the other, of small diameter, has a diaphragm, with tappings leading to a secondary flow meter and a pneumatic regulator controlling the valve on the first branch. This system can cope with a large range of flow rates.—T. E. D.

A New Machine for the Testing of Railway Wheels. J. N. Adcock. (*Rail. Steel Topics*, 1955, **3**, (1), 4–10).

Industrial Maintenance Steelworks Rolling Stock. G. C. Oram. (*Rail. Steel Topics*, 1954, **2**, (4), 17–26). A system of planned maintenance at the Appleby-Frodingham Steel Co. is shown to increase productivity.—A. D. H.

The Significance of Flow Phenomena in Iron and Steel Works. M. Hansen. (*Stahl u. Eisen*, 1955, **75**, Apr. 7, 401–410). The author discusses the importance of aerodynamics in plant and equipment of iron and steelworks and quotes a number of cases in which proper aerodynamic design results in a marked improvement of the plant. Model tests greatly assist the choice of optimum design. Application of correct dynamic design also results in substantial savings in power which are not only obtained by the few large installations in a works but by improving the performance of the numerous small units.—T. G.

New Method for the Disposal of Phenol-Containing Effluents from Gas Works. O. J. Stebel and H. Engelbrecht. (*Stahl u. Eisen*, 1955, **75**, Mar. 24, 346–348). The authors review the various methods used in the disposal of phenol-bearing effluents from gas works and describe a plant in which the phenol is disposed of by burning. The temperature needed for complete combustion of the phenol is about 800° C. The thermal energy needed in the pilot plant amounts to approximately 1000 kcal per l. of phenol-bearing water (phenol content 4–8 g/l).—T. G.

Modern Steel Plant Lighting: Installation at the Lackenby Works of Dorman Long (Steel), Limited. (*Iron Coal Trades Rev.*, **170**, 1955, Mar. 25, 688–689). Brief details are given of a number of interesting examples of modern industrial lighting included in the new Lackenby Works of Dorman Long (Steel) Ltd.—G. F.

New Mechanized Foundry in Scotland: Production of Colliery Conveyor Accessories. (*Iron Coal Trades Rev.*, 1955, **170**, Apr. 1, 745–746). A description is given of the layout of the new mechanized foundry of Mavor and Coulson Ltd., Glasgow, chiefly producing colliery conveyor accessories.—G. F.

Steam Generation at Scunthorpe. (*Steam Eng.*, 1955, **24**, Mar., 201–205). The new boiler and turbo-compressor plant at Appleby-Frodingham is described, with details of steam generating equipment, forced- and induced-draught equipment, and oil-fired furnaces. Water feed and control are mentioned, and automatic combustion control is outlined. Blast-furnace cleaning plant, the turbo-blowers, and generating equipment are briefly described.—T. E. D.

Performance of Wet Dust Scrubbers. C. E. Lapple and H. J. Kamack. (*Chem. Eng. Prog.*, 1955, **51**, Mar., 110–121). Laboratory- and pilot-scale investigations on the performance of cyclone, pipe-line, Venturi, and orifice contactors in the wet cleaning of gas are reported. Dusts of talc, ilmenite, and TiO_2 were studied, and operating variables *v.* dust losses are shown.

Electrically Operated Steelworks Cars: Equipment in Use at Shotton. (*Iron Coal Trades Rev.*, 1955, **170**, Mar. 25, 671–673). Three types of electrically operated wagons are used at John Summers and Sons, Ltd. One type is the hot-metal car used for transporting 60-ton ladles along the melting shop; the other two types, at the new blast-furnace plant, are the transfer cars taking ore from the high-line loading station to the bunkers, and the scale cars drawing and weighing the ore from the bunkers.—G. F.

Steam and Power Plant at Normanby Park Steel Works: Features of Post-War Development. C. L. Reid. (*Iron Coal Trades Rev.*, 1955, **170**, Mar. 11, 551–557). Details are given of the main features of post-war development in steam and power plant at Normanby Park Works of John Lysaght's Scunthorpe Works Ltd. The chief items of plant described are three 80,000 lb/h boilers and two 5000-kW back-pressure turbines.—G. F.

Statistics of Accidents in Works Transport in Iron and Steel Works. J. Dobrzanski. (*Hutnik*, 1955, **22**, (4), 120–124). (In Polish). On the basis of statistical analysis of accidents in four integrated steelworks during 1950–1953, the main causes of accidents in works' transport are discussed.—V. G.

Taper *v.* Straight Tread Crane Wheels. J. A. Bell. (*Iron Steel Eng.*, 1955, **32**, Feb., 55–60). The author describes research carried out on the subject of wheel treads by means of experimental evidence obtained on a ½ in./ft scale model crane permitting all combinations of wheel tread arrangements. The author concludes that where structures are not too rigid and where runways are long tapered treads are preferable.

Maintenance of Mobile Equipment. F. C. Wier and J. C. Lantz. (*Iron Steel Eng.*, 1955, **32**, Mar., 90–97). The maintenance procedure for automotive vehicles at the Timken Roller Bearing Co. is described. Details are given of the organization, maintenance policy and operational techniques. Reference is made to the use of V.H.F. radio.—M. D. J. B.

Material Handling in The Steel Plant—Plant Layout. G. L. Berry. (*Iron Steel Eng.*, 1955, **32**, Mar., 101–104). This paper describes certain fundamental concepts of material handling methods which have been adopted in the sheet and strip mills of the Ford Motor Co.—M. D. J. B.

Material Handling in the Steel Plant. Heating Facilities. W. A. Dailey. (*Iron Steel Eng.*, 1955, **32**, Mar., 104–107). This paper discusses in some detail the heating phases of steel production and the material handling problems associated with them. Particular reference is made to soaking pits, slab heating and annealing furnaces.—M. D. J. B.

Material Handling in the Steel Plant. Mill Equipment. G. Perrault, jun. (*Iron Steel Eng.*, 1955, **32**, Mar., 107–111). The author discusses improved methods of handling steel strip which have been successfully adopted at the Ford Motor Co.—M. D. J. B.

Choosing Electrical Equipment for Belt Conveyors. W. J. Heacock. (*Iron Steel Eng.*, 1955, **32**, Mar., 117–123). This paper discusses the electrical problems associated with long belt conveyors for materials handling. The electrical problems involve the application of motors, controls, safety devices and distribution of power.—M. D. J. B.

Communications in the Steel Industry. T. E. Hughes. (*Iron Steel Eng.*, 1955, **32**, Apr., 109–114). The relative merits and applications of loudspeaker systems, carrier phone systems and very high frequency mobile radio are discussed.—M. D. J. B.

New Adjustable Pincers for Soaking Pit Cranes. (*Berg. Hüttenmänn. Monatsh.*, 1955, **100**, Jan., 87–89). Details are given of pincers which have a maximum load capacity of 20 tons and can grip ingots between 30 cm and 1·70 m in width.—E. C.

Testing of Overhead Travelling Cranes. J. C. Pattison. (*J. S. African Inst. Mech. Eng.*, 1954, **3**, Jan., 179–189). The author describes factory tests on a 75-ton crane manufactured by a South African factory for the Electricity Supply Commission.—L. E. W.

Soviet Chip-Briquetting Plants. M. J. Kudrin. (*Giessereitechnik*, 1955, **1**, May, 68–71). Methods used in the U.S.S.R. for the production of briquettes from metal chips are discussed: presses are described and illustrated.—L. J. L.

Remote Control of Crane Installations. K. Kleinschmidt. (*Maschinenwelt u. Electrotechnik*, 1955, **10**, May, 128–130). The principles of remote control of cranes are outlined, and an example is given in detail of the application of remote control to cranes operated in a forge.—L. D. H.

New Forging Manipulator Reduces Handling Costs. (*Mech. Handling*, 1955, **42**, May, 286). A 2000 lb capacity forging manipulator designed to transfer the hot billet from the heat treatment furnace to the press or hammer and to manipulate it during the forging operation is described.—D. H.

Automation and Mechanization in the Steel Industry. G. P. Jones. (*Brit. Steelmaker*, 1955, **21**, Mar., 76–78). The author briefly reviews the latest techniques for extending mechanization and automatic control in various branches of the steel industry, and discusses the economic and human implications.—G. F.

Electrically Operated Steelworks Cars. J. Beasley. (*Metro. Vick. Gaz.*, 1955, **26**, Apr., 122–126). The construction, capacity, and operation are described of scale and transfer cars in use at the new blast furnace plant of John Summers and Sons. A hot-metal transfer car used in the melting shop is also described.—L. D. H.

Some Problems of Wear and Tear arising at the Royal Dutch Blast Furnaces and Steel Works at Ijmuiden. J. de Vries. (*Metalen*, 1955, **10**, May 31, 135–139). [In Dutch]. This paper deals with wear and tear problems peculiar to the equipment of a large steel undertaking, e.g. hawsers, pumps, railway wheels, bearings, piping, ducts etc., followed by a discussion and suggested remedies.—F. R. H.

LUBRICATION

Lubrication and Surface Treatment with Molybdenum Disulphide of the Highest Purity. F. Weiss. (*Werkstoffe Korrosion*, 1955, **5**, May, 250–252).

Metallic Friction and Lubrication by Laminar Solids—A Review of Current Theories. E. Koenigsberg and V. R. Johnson. (*Mech. Eng.*, 1955, **77**, Feb., 141–147). Modern theories of friction are described and critically discussed in the light of latest experimental work. Brief details are given of the use of solid lubricants, particularly graphite and molybdenum disulphide.—D. H.

On the Solution of the Reynolds Equation for Slider-Bearing Lubrication—VIII. The Optimum Slider Profile for Viscosity a Function of the Pressure. A. Charnes, F. Osterle and E. Saibel. (*Trans. Amer. Soc. Mech. Eng.*, 1955, **77**, Jan., 33–36). The problem of determining the optimum slider profile from the standpoint of load carrying capacity is solved for the case of isothermal flow—D. H.

Fluorine-Containing Lubricants. A. J. Rudge. (*Chem. Indust.*, 1955, Apr. 23, 452—461). The author describes methods of preparation and certain properties of various commercially available fluoro-lubricants and chlorofluoro-lubricants. They are chemically stable under difficult conditions but their use will probably be confined to special applications where lower cost lubricants are not so effective. In many cases the chlorofluorocarbons are as satisfactory as the more expensive fluorocarbons. (19 references).—E. E. W.

Aeration in Lubrication and Hydraulic Systems. B. R. Walsh. (*Product Eng.*, 1955, **26**, May, 136–139). The cause of aeration occuring in fluid systems is discussed, together with various ways of reducing its effect. An example is given of an ideal layout for a lubrication system.—E. A. C.

Practical Design Applications for Hydrostatic Lubrication. T. L. Corey and E. M. Kipp. (*Machine Design*, 1955, **27**, Mar., 189–196). Hydrostatic bearings have a number of important attributes which include: ability to provide a continuous film of lubricant between bearing surfaces under conditions where it is difficult or impractical to develop and maintain hydrostatic lubrication, phenomenally low coefficient of friction, increased lattitude in selection of suitable lubricating fluids and greater choice in selection of suitable bearing materials.

Lubricants for Press and Forge Equipment. A. A. Paul. (*Steel Processing*, 1954, **40**, Nov., 703–704, 738–739). A general review is given of the importance of adequate and regular lubrication of mechanical forging machines. The advantages of extreme pressure lead-base lubricants under the severe conditions encountered in the gears, slides, toggles and bearings of such equipment are discussed.—P. M. C.

Marking of Lubricant Containers, Lubricators, and Lubrication Spots in Iron and Steel Works. F. Wunsch. (*Stahl u. Eisen*, 1955, **75**, Mar. 10, 278–281). The author describes a scheme for distinctive marking of lubrication systems and lubricant containers.—T. G.

WELDING AND FLAME-CUTTING

High Temperature Alloy Brazing of Thin Materials for Jet Engines. A. S. Rose and W. N. Lewis. (*Welding J.*, 1955, **34**, Jan., 30–39). The use of brazing alloy powders (in particular AWS–ASTM BNiCr filler material and AMS 4775 material) for the fabrication of jet engine components for operation at elevated temperatures is discussed. Metallographic examination of the brazed joints shows that the relatively wide solidus–liquidus range (1850–1950° F) and the erosive penetration and alloying of the parent materials by the brazing material are of prime importance. Full utilization of capillarity for joint filling is demonstrated.—U. E.

A Production Application of Inert-Gas-Shielded Metal-Arc Welding of Mild Steel. J. L. Lang. (*Welding J.*, 1955, **34**, Jan., 23–29). The factors of cost and quality, as well as the problems of spatter, arc length, control of undercut, porosity, and proper fixturing, are discussed.—U. E.

1954 Adams Lecture.—The Toughness of Weldability. W. L. Warner. (*Welding J.*, 1955, **34**, Jan., 9–22). The toughness is characterized by the metal's ability to absorb energy while deforming plastically thus involving ductility together with load-carrying capacity. An appropriate evaluation of toughness facilitates the selection of base metals, welding procedures, and quality control tests.—U. E.

Measurement of Resistance-Welding Variables. J. E. Roberts. (*Brit. Welding J.*, 1955, **2**, Apr., 176–180). Methods of measuring current, time, and electrode force are described, ranging from simple single-purpose indicating instruments to more complicated systems for recording all three variables simultaneously. The majority of methods are particularly suitable for use in spot and projection welding, but may not be so easily applied to other resistance-welding processes, such as flash welding, etc.—U. E.

Oxy-Acetylene Pressure Welding of Aircraft Undercarriage Components. D. C. Brown and J. J. Wilson. (*Brit. Welding J.*, 1955, **2**, Apr., 160–171). This paper describes the preliminary experimental work leading to the production of a large pressure-welded undercarriage in high-tensile Ni–Cr–Mo steel welded to En 25 material.—U. E.

Practical Control in the Welding of Alloy Steels. I. C. Fitch. (*Brit. Welding J.*, 1955, **2**, Apr., 151–158). This paper is confined to a consideration of metal-arc welding as applied to alloy steels. It describes methods of determining whether a steel is weldable, and shows how to establish the conditions necessary to weld it successfully and practical methods of obtaining these welding conditions, concluding with methods of checking that the desired result has been achieved.—U. E.

Stainless Steels—Welding Summary. H. Thielsch. (*Welding J.*, 1955, **34**, Jan., 22s–30s). A review is presented of the

effects of alloying elements, and welding characteristics of commercial austenitic, martensitic, and ferritic stainless-steel grades.—U. E.

The Production of Weldless and Welded Rings. (*Machinery*, 1955, **86**, Mar. 4, 467–470). Methods employed by N. Hingley and Sons Ltd. in the manufacture of ferrous and non-ferrous rings for many different purposes are described.—M. A. K.

Up-to-Date Welding Electrodes. W. Hummitzsch. (*Schweisstechn.*, 1955, **9**, Mar., 26–28).

Straightening with the Flame. R. Pfeiffer. (*Schweisstechn.*, 1954, **8**, Dec., 133–141). A number of examples are given with illustrations in which straightening is carried out by means of a burner. Distorted welded fabrications and warped sheet metals are easily straightened by this method.—U. E.

Electrodes for Welding of Un-Alloyed and Low-Alloy Steels. A. Schmidt. (*Schweisstechn.*, 1955, **9**, Mar., 28–31). Welding electrode Standard DIN 1913 is discussed in detail.—U. E.

The Silox-Brazing Method for Cast Iron. B. Sixt. (*Schweisstechn.*, 1955, **9**, Jan. 1–5; Feb., 17–20). A new brazing process is described which can be applied to cast iron. For example, broken cast-iron pieces can be joined, cracks and cavities can be filled in. The method is divided into two operations. The first operation can be described as metallizing, i.e. a thin adherent layer of brass is laid on to the surface of the two pieces to be joined. In the second operation brazing is carried out in the usual way using a special filler metal and flux.

Auxiliary Devices and Equipment for Gas Cutting. H. von Hofe. (*Schweisstechn.*, 1955, **9**, Jan., 5–10; Feb., 21–24). Different gas-cutting equipment is described in detail. Auxiliary devices for all types of burner, for keeping the burner straight or at a certain height from the workpiece, are illustrated. Gas cutting of tubes, bent pipes, and a forged tank is discussed. With the help of auxiliary equipment, gas cutting can be applied successfully to difficult workpieces.—U. E.

Practical Steps Against Shrinkage Effects in Welded Constructions—Economic Aspects. R. Malisius. (*Schweissen u. Schneiden*, 1955, **7**, Apr., 119–133). Preventive measures are described which should help the welding engineer to control shrinkage stresses and deformations in welded structures.

Investigations on the Rate of Melting of Manual Arc Welding Electrodes. H. Frankenbusch. (*Schweissen u. Schneiden*, 1955, **7**, Apr., 140–143). The rate of melting was investigated and it was found that all amperages showed a scatter of $\pm$ 5% and melting capacities (measured in kg/h) showed a scatter of about $\pm$ 20%.—U. E.

Development and Present Position in Gas Welding of Rails. F. Fries. (*Schweissen u. Schneiden*, 1955, **7**, Apr., 144–151).

Considerations of Failures of Welded American Ships. H. Dohrmann. (*Schweissen u. Schneiden*, 1955, **7**, Apr., 151–158). A review is given of failures in welded ships, variable effect of riveting and welding on the susceptibility to failure.—U. E.

Contribution to the Formation of Titanium Nitrides in Weld Metal Deposits from Rutile Type Coated Electrodes. W. Hammitzsch and L. Hense. (*Schweissen u. Schneiden*, 1955, **7**, Mar., 79–85). The effect of the presence of nitrides on the quality of weld deposits has been investigated, and the influence of coat thickness and other constituents in the electrode coating such as ferro-manganese, cellulose, aluminium compounds, and alkaline compounds is discussed.

The Repair of Crane Travelling Wheels by Means of the Union Melt Welding Process. (*Schweissen u. Schneiden*, 1955, **7**, Mar., 107–109).

A Critical Survey of Brittle Failure in Carbon Plate Steel Structures other than Ships. M. E. Shank. (*Weld. Res. Council Weld. Res. Council Bull. Series*, 1954, Jan., (17)). An extensive review is given of 64 structural failures and failures in gas-transmission lines, in both riveted and welded structures, such as tanks, bridges, pressure vessels, power shovels, etc. Brittle failure in these structures is the same phenomenon as occurs in ships.—U. E.

MACHINING AND MACHINABILITY

The Problem of the Stressed State of the Surface Layer of Steel During Machining. V. N. Timofeev. (*Zhurnal Tekhnicheskoi Fiziki*, 1954, **24**, (7), 1273–1281). [In Russian]. Residual stresses in the surface layers during high-speed cutting of steel were determined. The main stresses were neither tangential nor axial but were turned by a certain angle in relation to the axial symmetry of cut rings.—V. G.

Flexible Tools Aid Fabrication. J. Delmonte. (*Steel*, 1955, **136**, Mar. 7, 129–133). Examples of the use of plastic tools in metal forming are given. Mixtures of epoxy resin and synthetic rubber are used to give the required plasticity. One instance is the inclusion of a removable plastic 'snake' to support thin-walled tubes during forming and bending.—D. L. C. P.

CO_2 as a Machining Coolant. J. Grindrod. (*Canad. Metals*, 1955, **18**, Jan., 39–42).

Machining of Wire-Drawing Dies from Sintered Carbides by Electrosparking. Z. Steininger. (*Hutnik*, 1955, **22**, (3), 87–89). [In Polish]. Advantages of electrosparking machining over mechanical grinding are discussed and a description of the equipment used is given.—V. G.

Method of Measuring the Efficacy of Cutting Lubricants. P. Eugene. (*Rev. Univ. Min.*, 1955, 9th series, **11**, Mar., 101–110). A description is given of a new apparatus for measuring the efficacy of cutting oils. The results obtained during studies of the influence of 16 types of lubricant on temperature of cutting, tool wear, and cutting speed are reported.—B. G. B.

Turning En. 25T with Liquid CO_2 as a Coolant. (*Machine Shop Mag.*, 1955, **16**, Feb., 69–74). Research into the use of liquid CO_2 as a coolant for machining has led to development of the 'Ce De Cut' technique by the Carbon Dioxode Co. The technique of its application in turning is different from that of normal fluids.—M. A. K.

Russians Report.—Ceramic-tipped Tools Cut Steel. (*Machinist*, 1955, **99**, Feb. 25, 326–328). The article briefly describes research and development carried out in U.S.S.R. since 1947 to discover practical ceramic tools for use on an industrial scale.

Bibliography

Machining of Metals with Thermocorundum Cutting Edges. G. F. Kudsov. (*Stanki Instrument*, 1952, No. 5, pp. 14–16).

Machining of Metals with Thermocorundum Cutting Tools. P. P. Grudov and M. P. Tsiganova. (*Stanki Instrument*, 1952, No. 4, pp. 12–14; *also Engineers Digest*, 1953, Feb., pp. 59–62).

Applications of Ceramic Materials for Machining Metals. A. I. Isaev, N. N. Zorev, and L. K. Luchma. *Ibid.*, 1952, No. 4, pp. 12–14: *also Engineers Digest*, 1953, Feb., pp. 51–52.

New Material for Tool Cutting Tips. M. M. Karatygin and N. F. Kasakov. (*Vestnik Mashinostroeniya*, 1952, No. 4, pp. 87–90.)

Hardness of Tungsten Carbide and Mineral-Ceramic Materials at Elevated Temperatures. A. I. Betaneli. *Ibid.*, 1953, No. 4, pp. 49–52.

New Machining Tests with Ceramic Cutting Edges. A. Richter. (*Fertigungtechnik*, 1952, No. 10, p. 291).

Cutting Tools with Sintered Corundum Tips. J. Koloc. (*Strojirenstvi*, 1952, No. 1, pp. 48–49).

Influence of the Properties of Corundum on the Design of Tool Holders. F. Vintner. (*Stro-irenska Vyroba*, 1954, No. 3, pp. 100–102).

PROPERTIES AND TESTS

Deformation and Ageing Phenomena in the Torsion Testing of Cold-Drawn Mild Steel Wire. A. Josefsson. (*Jernkontorets Ann.*, 1955, **139**, (4), 225–249). [In Swedish]. The influence of nitrogen on torsion testing of cold-drawn wire was investigated, fine 5-mm dia. steel samples having 0·002–0·03% C being drawn down in 13 passes to 0·7 mm, with 20–30% pass reduction. Torsion tests were carried out after each pass to establish the number of turns required to achieve fracture of a length (100 × wire diameter). If the nitrogen content exceeded 0·008%, the number of turns required after the third or fourth pass was greatly reduced, possibly owing to instantaneous ageing. In the case of the highest percentage reductions the torsion test results seemed less dependent on nitrogen content. Factors affecting the mechanism of deformation are discussed.—G. G. K.

Ageing of High Carbon Steel. D. S. Kazarnovskii. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (9), 1636–1643). [In Russian]. The nature of ageing of high-carbon steel and the influence of its phase state and the degree of carbide dispersion on the tendency of steel to ageing was studied. It is concluded that steels containing 0·6% C or more could be prone to ageing. After a deformation and a subsequent heating the impact strength could be decreased by up to 50% of the initial value.

A relatively lower sensitivity of high-carbon steels to ageing can be explained by the fact that carbon strongly decreases the diffusion of nitrogen in iron. The nature of the mechanical ageing of high-carbon steels is similar to that of low-carbon ones. Ageing processes (dispersion hardening) take place in both structurally free ferrite as well as in ferrite incorporated into the structure of pearlite.—V. G.

Brittle Fracture: Test Methods and Theoretical Considerations. T. S. Robertson. (*Iron Steel*, 1955, **28**, May, 161–166). The author gives a brief history of methods of determining the susceptibility of steels to brittle fracture, and describes the development of a method simulating more closely the actual conditions of failure.—G. F.

Brittle Failure of Steel Structures. M. E. Shank. (*Metal Progress*, 1955, **67**, June, 111–121). A survey is presented of recent published work on brittle fracture of steel. Theoretical studies have enabled the plastic work, modulus of elasticity, and size of crack to be correlated with the stress required for a catastrophic crack.—B. G. B.

The Geometrical Size Effect in Notch Brittle Fracture. A. A. Wells. (*N.E. Coast Inst. Eng. Shipbuilders*, Preprint, 1955, Mar. 25, 276–290). Failure size effects are discussed in the light of metallurgical and geometrical variables and the Docherty size effect in slow notched-bar bend testing is cited as an example of a purely geometrical effect. Repeat tests show that this effect is present when metallurgical differences between specimens are reduced as far as possible. Brittle fracture in the smallest of the geometrically similar specimens is prevented by there being an insufficient strain-energy release rate and that the critical value is close to values determined for the same steel and temperature by the notched bar test. The implications of such a criterion for propagation are then discussed in relation to the fracture of full-scale welded steel plate structures.—B. G. B.

X-ray Microbeam Studies of Brittle Fractures in Metals. Lo-Ching Chang. (*J. Mech. Phys. Solids*, 1955, **3**, Apr., 212–217). It is concluded from X-ray microbeam studies of fracture surfaces of polycrystalline iron and zinc, that brittle fracture is preceded by plastic deformation with the crack nucleated by plastic strain. The determining factor for crack propagation is the plastic work factor and not the surface energy, the former being about two magnitudes larger than the latter. The critical crack length as defined by the Hall–Griffith formula for crack propagation is of the order of the grain diameter.—J. G. W.

Accelerated Wear Test. A. Secciani. (*Met. Ital.*, 1955, **47**, Feb., 63–68). [In Italian]. The author suggests an accelerated wear test which will yield comparable results to long-term testing and examines the possibility of establishing one conclusive factor which can be used to determine resistance to wear. Suggestions are put forward for a specification for accelerated wear tests.—M. D. J. B.

Influence of Vanadium and Molybdenum on the Properties of Air-hardening Die Steel Containing 1·5% Carbon and 12% Chromium. N. Yamanaka and K. Kusada. (*Tetsu to Hagane*, 1955, **41**, June, 613–620). [In Japanese]. Results are given of investigations of the alterations in critical point, hardenability, quenched and tempered hardness, retained austenite, dimensions and toughness produced by V and Mo additions to a steel containing 1·5% C and 12% Cr.—K. E. J.

Investigation of Boron Steel. I. S. Koshiba, K. Tanaka, and A. Inata. (*Tetsu to Hagane*, 1955, **41**, June, 601–607). [In Japanese]. Investigations were made on Cr and Cr–Mo case-hardening steels and high-tensile steels. In the range 0·001–0·011% B, the Ac_3 points were raised and Ar lowered. Approx. 0·003% B gives the optimum increase in hardenability. At tempering temperatures of 200–300° C, the quenched and tempered hardness of B-containing steels was increased, but no effect was observed at 400° C. Addition of B increased tensile strengths; the impact value of case-hardening steel was reduced, and of high-tensile steel increased slightly. The carburizing depth was slightly increased.

Effect of Various Alloying Elements on the Mechanical Properties of Heat-Resisting Austenitic Chromium–Nickel Steels at Temperatures Between 600 and 700° C. H. A. Vogels. (*Stahl u. Eisen*, 1955, **75**, May 5, 559–570). Nineteen experimental alloys based on Nb-bearing 16% Cr–13% Ni steel with increasing amounts of cobalt (up to 20%) with and without carbide formers such as Mo, W, V, and N were prepared and hot-working properties, creep behaviour, embrittlement tendency, and scale resistance studied in the range 600–700° C. Melting practice (particularly deoxidation) affects the hot-working properties of the steels. No effect of the creep properties with increasing addition of alloying elements was found, but high-temperature tensile strength rises markedly with addition of Mo, W, and V to Co-free steels. Studies of microcrack sensitivity of austenitic welds showed a clear relation to the hot-working properties.—T. G.

Influence of Aluminium on the Structure, Size of Austenite Grain and Toughness of Medium-Carbon Steel. I. S. Gaev and V. V. Polovnikov. (*Liteĭnoe Proizvodstvo*, 1955, (1), 18–21). [In Russian]. In the investigation described the importance of structure in the change of the properties of steel brought about by Al was studied. Specimens of untreated steel and of steel to which 0·02%, 0·06%, 0·1%, and 0·2% Al had been added were carburized, decarburized, annealed, or vacuum-heated under various conditions and the resulting grain sizes and mechanical properties studied. The austenite grain size affected mechanical properties only when the effect of non-metallic inclusion was excluded. It is concluded that the net effect of various factors on toughness can be estimated by calculation from quantitative characteristics.—S. K.

A Relaxed Vacancy Model for Diffusion in Crystalline Metals. N. H. Nachtrieb and G. S. Handler. (*Acta Met.*, 1954, **2**, Nov., 797–802). The diffusion mechanism proposed envisages small regions of disorder within the crystal, consisting of 12–14 atoms relaxed inwards around a lattice vacancy.

The Effect of Short-Time Moderate Flux Neutron Irradiation on the Mechanical Properties of Some Metals. F. W. Kunz and A. N. Holden. (*Acta Met.*, 1954, **2**, Nov., 816–822). Single crystals of iron (Pb and Zn) were irradiated by neutrons in a reactor. The flow stress increased markedly after exposure. After annealing at 225–300° C the yield stress fell but after remained greater than that of an unirradiated control specimen. Radiation hardening mechanisms are critically reviewed.

Thermal Diffusion in Solid Alloys. L. S. Darken and R. A. Oriani. (*Acta Met.*, 1954, **2**, Nov., 841–847). The Ludwig–Soret effect was investigated in Fe–N, Fe–C, and Au–Cu alloys by annealing specimens under a temperature gradient; C and N migrated to the higher-temperature region of the specimen. The results are interpreted on a thermodynamic basis and, using a vacancy mechanism of diffusion, an explicit relation is developed to rationalize the results.—A. D. H.

Elastic Description of a High-Amplitude Spherical Pulse in Steel. W. A. Allen and W. Goldsmith. (*J. Appl. Phys.*, 1955, **26**, Jan., 69–74). A detailed description is given of the general method of the evaluation, using an electronic calculator, of a problem in elasticity that simulates the effect of a cylindrical charge of high explosive detonated in intimate contact with a steel plate.—E. E. W.

The Influence of Graphite Segregation on the Damping Capacity of Cast Iron. G. Cola. (*Fonderia Ital.*, 1955, **4**, Mar., 113–148). [In Italian]. The author makes a comprehensive review of the various theories dealing with internal friction in metals and considers the effects of non-homogeneity, nature of stress, magnetism, plasticity, and viscosity. The effects of graphite segregation on the characteristics of cast iron are examined in detail, particularly its resistance to mechanical wear and chemical corrosion, workability, resistance to fatigue, weldability, and sensitivity to notching. The experimental techniques adopted in these studies are described and a full account of all experimental results is given.—M. D. J. B.

The Texture of Deformation of Tubes from Stainless and Heat-Resistant Steel. Ts. N. Pafalovich. (*Zhur. Tekn. Fiz.*, 1954, **24**, (7), 1282–1287). [In Russian]. The texture of deformation of tubes from steels *X*28 (C 0·12%, Mn 0·6%, Cr 28%, Si 0·6%) and 1*X*18 *N*9*T* (C 0·11%, Mn 0·63%, Cr 18%, Si 0·59%, Ni 9·0%, Ti 0·5%) was studied. Modes of deformation used were consistent with present methods of production of thin-walled tubes.—V. G.

The Yield of Low Carbon Steels. V. A. Gladkovskii. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (6), 1090–1092). [In Russian]. Experimental investigation of the dependence of the yield of low-carbon steels on their carbon content is described.—V. G.

A Diameter Gauge and Dynamometer for True Stress–Strain Tension Tests at Constant True Strain Rate. G. W. Powell, E. R. Marshall, and W. A. Backofen. (*Amer. Soc. Test. Mat.*, Preprint, 1955, No. 88). A description is given of this apparatus, and typical data are reported from tests on an austenitic stainless steel and several strain rates at temperatures of 20–196° C.—B. G. B.

The Nominal Cleavage Strength of Steel. T. Noren. (*Jern-*

kontorets Ann., 1955, **139**, (3), 141–153). [In Swedish]. A brittle alloy was welded on the edges of several mild-steel test plates. When these were subjected to tensile tests fresh cracks were formed continuously in the brittle surface layers. At a certain critical stress such cracks will continue into the steel, resulting in brittle fracture. The nominal cleavage strength is defined as the highest nominal stress which a steel can resist in the presence of a crack without fracturing.

Static or Dynamic Pressure Electrically Measured. E. H. W. Banner. (*Machinery Lloyd, European Ed.*, 1955, **27**, Mar. 19, 45–48).

Comparison between Impact Test Pieces as used in the Schnadt and Charpy Methods. C. Schaub. (*Jernkontorets Ann.*, 1955, **139**, (5), 326–346). [In Swedish]. With the object of making such a comparison, a photoelastic investigation was undertaken of stress distribution in model test pieces, followed by an assessment of the impact resistance ratings of several carbon steels when tested using the two methods. Notch-toughness/temperature curves were plotted for seven steels having a breaking stress of 35–50 kg/mm², these being tested in the normalized and normalized/deformation-aged conditions. The comparison was based on determinations of the position of the lower limit of the transition range, defined as the highest temperature at which no single impact value exceeds 2 kg/cm². The results showed that Schnadt test pieces have certain disadvantages, but are on the whole equivalent to other types.—G. G. K.

Behaviour and Effect of Ferrite in Mixed Structure together with Cementite during Cold Working by Drawing and Rolling. H. Wedl. (*Draht, German ed.*, 1955, **6**, Jan., 1–7). Wires made of 0·05%, 0·46%, and 0·72% C steel were drawn and flattened by rolling and the change in mechanical properties was studied as a function of cold work and of the microstructure which ranged from almost pure ferrite through a dispersion of fine-grained cementite in a ferrite matrix to a homogeneous sorbitic structure characteristic of patented near-eutectoid. Observed differences in work hardening are interpreted in terms of the different mechanical properties of ferrite and cementite, and their interaction at interfaces of ferrite and cementite grains. The latter factor is invoked particularly in the case of deformation unaccompanied by hardening.—J. G. W.

A Practical Strain-Hardening Function. E. Voce. (*Metallurgia*, 1955, **51**, May, 219–226). An equation proposed by the author in 1948 to express the strain-hardening relationship is discussed in relation to subsequent research work.—B. G. B.

An Investigation of the Contraction Ratio of Work-Hardening Materials. A. Shelton and H. Ford. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 160–168). [This issue].

Criteria for Selecting Experimental Stress Analysis Methods. A. J. Durelli and E. A. Phillips. (*Product Eng.*, 1955, **26**, Jan., 182–191). An important phase of any experimental stress analysis is that of determining which of the many techniques available should be used. As a guide in such problems, nine basic concepts are described and the most important stress-analysis methods are then evaluated with these concepts in mind. Some attention is paid to combining experimental techniques and the significance of measurements.—A. M. F.

An Experimental Study of Biaxial Stress–Strain Relations in Plasticity. P. M. Naghdi and J. C. Rowley. (*J. Mech. Phys. Solids*, 1954, **3**, Oct., 63–80). Results of tests on 24S-T4 aluminium alloy tubes with severe initial anisotropy, which are described in the paper, are discussed with reference to initial and to strain-hardening anisotropy.—J. G. W.

The Effect of Microstructure on the Morphology of Fracture —Part I. J. C. Danko and R. D. Stout. (*Welding J.*, 1955, **34**, Mar., 113s–116s). The deformation of pearlite was investigated and it was found that the deformation above the transition temperature occurs by the formation of very broad kinks. In the transition temperature and below, extremely sharp kinks appear, initiating fracture, and are associated with brittle behaviour. Finer pearlites deform by much sharper kinks, the breadth of which decreases with decreasing interlamellar spacing of the pearlite.—U. E.

Energy Criteria of Fracture. E. Orowan. (*Welding J.*, 1955, **34**, Mar., 157s–160s). It is shown that for fully brittle materials, the Griffith equation represents a necessary and sufficient condition of tensile fracture. The author has modified the equation so that it can be applied to fast crack propagation in low-carbon steels, but the Griffith energy principle cannot be applied to ductile fracture except when the plastic deformation is confined to a thin layer of material at the surface of fracture.—U. E.

Effect of Subcritical Cooling Rate on the Brittle-Fracture Characteristics of Structural Steel. L. Mair. (*Trans. Amer. Inst. Min. Met. Eng.*, 1954, 200; *J. Met.*, 1954, **6**, Nov., Section 1, 1206–1207). The author has investigated the effect of cooling rate below 1200° F on the properties of structural steel, basing his evaluation of the brittle-fracture properties on the transition temperature of the Kahn tear test. The results indicate that pile cooling of structural steel plates below 1100° F is not detrimental to the brittle-fracture properties.—G. F.

Predicting Fatigue Failures of Notched Specimens. R. A. Miller. (*Product Eng.*, 1955, **26**, Apr., 177). A stress/strain curve for the material being considered is plotted, and from this, for any required value of stress, the effective stress raiser can be determined, and hence the peak stress can be calculated. Knowing the peak stress, for a given range of stress the number of cycles to fatigue can be obtained directly from an S/N curve for an un-notched specimen of the same material.—E. A. C.

The Variation of the Influence of the Structural Factors on the Cyclic Strength of Steel. I. L. Mirkin and E. D. Tsypkina. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (12), 2209–2216). [In Russian]. The influence of the various structural states of ferrite on its cyclic strength were investigated. It was established that changes of structure caused by alloying low-carbon steel in a tempered state produce simultaneous increase in the static and cyclic strength and that the grain size of ferrite has no practical influence on the strength limit.—V. G.

Testing of Wire in Fatigue. H. Oschatz. (*Draht, German ed.*, 1955, **6**, Jan., 12–13). Various fatigue-testing machines, manufactured by Schenck, for the testing of wire and springs are briefly described, including the rotating-bend test, pulsating push-pull test, and a programmed variable amplitude test.—J. G. W.

Fatigue Testing of Steel Wires. M. Hempel. (*Draht, German ed.*, 1955, **6**, Apr., 119–129, May, 178–183). In this survey the author discusses the following topics: testing machines; steels used in rope, spring, and reinforced concrete wires; endurance curves of patented steel wires; effect of method of manufacture, heat-treatment, size, composition, surface condition, surface coatings on the fatigue properties of wires; fatigue tests of wire products including coil springs, wire ropes, prestressed concrete wire structures.—J. G. W.

Studies on the Fatigue of Steels by the Magnetic Method. I. T. Ueda and M. Tanaka. (*Nippon Kinzoku Gakkai-Si*, 1951, **B, 15**, July, 289–292). [In Japanese]. Magnetic measurements were made on five annealed C steels, compared with standard specimens. No differential hysteresis loops were found when the imposed rotary stresses were below the fatigue limit, but the area of loops and the residual magnetism decreased, above the limit, as the number of cycles increased.—K. E. J.

Studies of the Biaxial Fatigue Properties of Pressure Vessel Steels. C. E. Bowman and T. J. Dolan. (*Welding J.*, 1955, **34**, Jan., 51s–59s). The effect of welding, notches, attachments, and fabrication on the plastic biaxial fatigue properties of pressure-vessel steels was investigated. The steels exhibited a slight increase in fatigue notch sensitivity with increased strain range per cycle. The ratio of fatigue strength to tensile strength decreased as the tensile strength increased.—U. E.

The Relation between the Microstructure and the Fatigue Strength of Ductile Cast Iron. Y. Masuko. (*Sumitomo Metals*, 1954, **6**, Oct., 207–213). [In Japanese]. Results are given for the fatigue strength and other properties of pearlitic, ferritic, and intermediate types of ductile iron, flaky iron, forged steel. High values of the strength and endurance limit of the intermediate (' bull's eye ') structure are attributed to reduction of the internal notch effect of the spheroidal graphite by the surrounding ferrite bands.—K. E. J.

High-Temperature Creep Testing Units for Heat-Resisting Steels. M. Sumitomo, T. Hasegawa, and O. Ochiai. (*Sumitomo Metals*, 1954, **6**, Oct., 214–225). [In Japanese]. Six units for performing creep and creep rupture tests at 400–900° C are being used to examine 19–9 DL, Timken 16–25–6, S816, and other alloys.—K. E. J.

The Nature of Hardness of Hardened Steel. G. V. Kudryumov. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (7), 1254–1267). [In Russian]. The problem of a high resistance to plastic deformation of

hardened steels is discussed. The following basic views on the subject are considered: work hardening due to austenite–martensite transformation, high hardness of martensite crystals, and the presence of dispersed carbide particles caused by the decomposition of the supersaturated solid solution.—V. G.

New Portable Hardness Testers. (*Metallurgia*, 1955, **51**, Mar., 155–156). A brief account of two new portable Vickers hardness testers is given: one is for general testing and the other for testing on the pitch line of large gear wheels.—B. G. B.

Strain-Ageing and the Yield-Point Elongation of Low-Carbon Steels. H. P. Tardif. (*Canad. Min. Met. Bull.*, 1954, **47**, Dec., 797–803: *Trans. Canad. Min. Met.*, 1954, **57**, 499–505). The activation energy and the kinetics of strain ageing of stretching and temper-rolling treatments were determined, and also the factors that may intervene to cause a slower rate of return of the yield point in temper-rolled specimens. A low-carbon rimmed steel was used in the investigations. Activation energies obtained from hardness, yield strength, and yield-point elongation measurements, are of the same order of magnitude as those for the diffusion of carbon and nitrogen in iron.—T. E. D.

The Effect of Tungsten on Susceptibility to Temper-Brittleness. J. Čadek. (*Hutnické Listy*, 1955, **10**, (5), 285–293). [In Czech]. The efficiency of tungsten in preventing temper brittleness in 35% Cr–2% V and in 15% Ni–4% Cr low-alloy steels was investigated and compared with similar effects obtainable with molybdenum, primarily with a view of replacing the latter by the former. Replacement was found to be possible, optimum tungsten additions being at most 80% greater than the molybdenum additions prescribed by Czechoslovak Standard Specifications.—P. F.

The Effect of Microstructure on Notch Toughness—Part II. J. H. Gross and R. D. Stout. (*Welding J.*, 1955, **34**, Mar., 117s–122s). The notch toughness of plain carbon eutectoid and a slightly hypereutectoid steel was investigated, and it was found that generally the notch toughness of eutectoid steel decreases as the transformation temperature is lowered and the fineness of the microstructure increases. A marked tendency toward spheroidization was observed, which favours notch toughness.—U. E.

Blistering and Embrittlement of Pressure Vessel Steels by Hydrogen. G. A. Nelson and R. T. Effinger. (*Welding J.*, 1955, **34**, Jan., 12s–21s). The nature of the damaging effects of hydrogen on pressure-vessel steels is investigated and found to vary with temperature and other factors, particularly the presence of certain corrosives. At high temperatures and pressures atomic hydrogen from thermal dissociation causes decarburization and general intergranular cracking. At low temperature, diffusion of atomic hydrogen produced by corrosion leads to blisters and isolated cracks. Precautions to be taken against damage of both of these kinds are outlined for use in design construction and operation of pressure vessels.

Brittle Fracture in Steel. C. P. Oldridge. (*Welding Metal Fab.*, 1955, **23**, Feb., 55–60; Mar., 103–111).

Temper Brittleness of Pressure Vessel Steels. L. D. Jaffe. (*Welding J.*, 1955, **34**, Mar., 141s–150s). The temper brittleness of pressure-vessel steels has been investigated and the author has collected all available published information. The severity of temper brittleness in the low-carbon pressure-vessel steels commonly used in the as-rolled or normalized condition is generally not appreciable. The effect may be more severe in the quenched and tempered low-alloy steels, particularly those containing manganese, chromium, or nickel.—U. E.

Analysis of the Effect of Various Factors on Metal Transfer and Wear between Specimen Pairs of Same Metal and Same Shape—II. Effect of the Surrounding Atmosphere. I-Ming Feng. (*J. Appl. Phys.*, 1955, **26**, Jan., 28–32). Discussion is limited to the case where the surface layer is thinner than the average depth of roughening. The effect is discussed qualitatively.—E. E. W.

Permanent Magnets. (*Edgar Allen News*, 1954, **33**, June, 121–123; July, 155–156; Aug., 177–178; Sept., 203–204; Oct., 226–227; Nov., 253). Information is given on permanent magnets including their theory, design, materials, production.

Some Properties of Nickel–Zinc Ferrites. L. I. Rabkin and B. Sh. Epshtein. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (9), 1568–1578). [In Russian]. Results of experimental investigations of magnetic properties of nickel–zinc ferrites of various composition ($m\ NiFe_2O_4 + n\ ZnFe_2O_4$) and their dependence on the intensity and frequency of the field and temperature are given. Properties of ferrites in pulsating fields and their dependence on the frequency and amplitude of the variable component of the field and the intensity of the constant component of the field were also considered.—V. G.

Measurement of the Dynamic Magnetostriction in Transformer Alloy Stampings. W. Alexander. (*Beama J.*, 1955 **62**, Feb., 84–86).

Temperature Dependence of Magnetic Properties of Nickel–Zinc and Copper–Zinc Ferrites. A. I. Suchkov. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (9), 1579–1583). [In Russian]. Experimental investigation of the dependence of the main magnetic properties of ferrites $(NiO, ZnO)Fe_2O_3$ and $(CuO, ZnO)Fe_2O_3$ on temperature are described. Temperature-dependence of magnetic properties of ferrites and metallic ferromagnetics is governed by the same laws.—V. G.

Improvement of Magnetic Properties of Iron for Transformers by an Electrochemical Treatment. S. Ya. Grilikhes. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (10), 1786–1787). [In Russian]. The influence of an electrochemical treatment of iron for transformers on its magnetic permeability and hysteresis losses was investigated. The treatment consisted of immersion of iron specimens as anodes into electrolyte (orthophosphoric acid 65%, sulphuric acid 15%, chromic anhydride 6%, water 14%) for 10 min. During this treatment the layer of metal of about 70–80μ thick is dissolved and a passive film is formed on the surface of the metal, which prevents etching. This treatment increases the magnetic permeability and decreases hysteresis losses, which is explained by a decrease in the influence of work hardening due to anodic solution of the surface layer of the metal.—V. G.

Non-destructive Inspection of Steel Wire. I. Inspection of Heat-Treated Steel Wire. T. Ueda and K. Asakura. (*Nippon Kinzoku Gakkai-Si*, 1951, **B, 15**, Nov., 553–558). [In Japanese]. Details are given of a pilot apparatus for examining fine wire, designed on the basis of testing induced magnetic flux, since permeability varies with the heat-treated structure.—K. E. J.

Steels and Alloys for Permanent Magnets. A. H. Michel. (*Doc. Mét.*, 1954, Oct.-Nov.-Dec., 155–177). The principles of magnetism as applied to permanent magnets are summarized, and the theory of the permanent magnet and the modern theory of magnetism reviewed. The steels and alloys which can be used to make permanent magnets are then considered, and among the materials dealt with are quenched high-carbon steels, and alloys based on Fe–Ni–Al, Cu–Ni–Fe, Co–Fe, Fe–Mo–Co, and silver. The properties of magnets made from powdered metals and those based on ferrites are also reviewed.—B. C. W.

Magnetic Measurements during Tension. P. E. Lagasse. (*Rev. Mét.*, 1955, **52**, Jan., 28–32). The author shows that in a steel test piece under tension the variation in apparent magnetic permeability can be used to interpret, to some extent, structural modifications of the metal. This measurement, in the region of elastic deformations, makes particularly apparent certain irreversible modifications corresponding to a degree of known tension.—G. E. D.

The Heat-Transfer Coefficient for Heating Steel in a Lead Bath. H. Bühler. (*Stahl u. Eisen*, 1955, **75**, Apr. 7, 420–421). The author found the following values for the heat-transfer coefficient for steel parts immersed in a lead bath of initially 450° and 750° C, respectively: $\alpha_{450} = 1112$, $\alpha_{750} = 1075$ kcal/m² h °C. Heating the 273-g steel parts to 400° and 650° C, respectively, took about 50 s.—T. G.

How to Check Accuracy of Ultrasonic Flaw Detection. N. Grossman. (*Mat. Methods*, 1954, **40**, Nov., 100–101). The use of reference blocks containing drilled holes of various diameters is described as a direct and simple method of setting ultrasonic inspection equipment to detect flaws above a specified minimum size.—P. M. C.

ABC's of Ultrasonic Inspection. E. F. Weller, jun. (*Steel Processing*, 1954, **40**, Nov., 706–714). The pulse echo and transmission techniques for internal flaw detection, and the resonance method for thickness measurements are described. Fundamental principles of these applications of ultrasound are outlined and many examples, with illustrations, are given of their uses.—P. M. C.

Industrial Control by Ultrasonics. J. Daurat. (*Mét. Constr. Mécan.*, 1954, **86**, Sept., 653–658; Nov., 851–857; Dec., 933–943; 1955, **87**, Jan., 31–34; Mar., 191–195; May, 379–385). The location of internal defects by the reflection and transmission of ultrasonic waves in materials is described. The

production of ultrasonic waves and the interpretation of cathode-ray oscilloscope traces are explained. The use and design of the 'reflectroscope' is described.—B. G. B.

Progress in Flaw Detection. (*Min. J.*, 1955, **244**, May 27, 591). A brief account of the Minchom Sempun magnetic flaw detector is given.—B. G. B.

Gamma-Radiography in Industry. D. J. Griffiths. (*English Elect. J.*, 1955, **14**, Mar., 8–22).

New Applications of Ultrasonics in the Study of Materials. J. Brigg. (*Rev. Univ. Min.*, 1955, 9th series, **11**, Apr., 137–151).

New Possibilities of X-ray Examination of Steel. G. Lang. (*Z.VdI*, 1955, **97**, Apr. 15, 347–350). A technique for visual examination of X-ray transmission images projected on to a screen is described. Its use for fairly thick steel objects is made possible by an X-ray intensifier.—J. G. W.

A Tungsten Coil Furnace for High-Temperature X-Ray Diffraction Investigations. I. J. McKeand and R. K. Hursh. (*J. Amer. Ceram. Soc.*, 1955, **38**, Feb., 63–65). With the furnace described, X-ray spectrometer data can be obtained up to 2000° C, provided the specimens are not readily affected by tungsten vapours.—D. L. C. P.

The Use of Beta-Rays in Industry and Technology. H. G. Fendler. (*Draht, German ed.*, 1955, **6**, Apr., 136–140). The source, properties, and measurement of β-rays and their many technical applications are described, including measurement of coating thickness and gauging of foil.—J. G. W.

Standard X-Ray Diffraction Patterns. Vol. IV. H. E. Swanson, Ruth K. Fuyat, and G. M. Ugrinic. (*Circ. Nat. Bur. Stand.*, No. 539, 1955, Mar., 75 pp.). X-ray diffraction for a further 42 inorganic substances are included in this volume, together with a cumulative index for all the previous volumes.

Apparatus for Measuring the Thermal Conductivity of Metals in Vacuum at High Temperatures. M. Moss. (*Rev. Sci. Instruments*, 1955, **26**, Mar., 276–280). The apparatus permits the direct measurement of the thermal conductivity of metals at temperatures up to 700° C, without comparison with standard metal specimens. Axial heat flow is measured, the heat flux being measured by a constant-flow water calorimeter.—L. D. H.

METALLOGRAPHY

The Polygonization of High-Purity Iron. J. Talbot, C. de Beaulieu, and G. Chaudron. (*Rev. Mét.*, 1954, **51**, Dec., 839–844). It has been shown by X-ray and micrographic methods that the temperature at which polygonization occurs is lowered as the purity of the metal increases. As a result, the premature appearance of polygonization delays the process of primary crystallization. It follows that the method of critical cold working cannot be used to obtain high-purity monocrystals of iron. Furthermore, the polygonized state of pure iron appears as the stable state in the evolution of cold worked iron.—G. E. D.

Autoradiographic Study of the Influence of Forging on the Dendritic Segregation of Phosphorus in Steel Ingots. A. Kohn and J. Doumerc. (*Rev. Mét.*, 1955, **52**, Mar., 249–259). An experimental steel ingot containing 0·42% C and 0·021% P was inoculated with ferrophosphorus. The influence of forging and slow annealing at temperatures between 1100° and 1300° C on the segregation of phosphorus was studied using autoradiographic methods. The rate of homogeneity was deduced and a quantitative estimation of the factors studied could be made.—G. E. D.

Internal Friction of Metal Single Crystals. J. Weertman. (*J. Appl. Phys.*, 1955, **26**, Feb., 202–210). The author calculates the portion of the interval friction of annealed and moderately cold-worked metal crystals due to dislocation motion and shows that Nowick's qualitative theory leads to reasonable orders of magnitude of the observed internal friction and changes in Young's modulus. At low temperatures or at high frequencies the mechanism becomes inoperative.—E. E. W.

Super Cooling and Germination Preceding the Solidification of Alloys. (*J. Inf. Tech. Indust. Fonderie*, 1955, Mar.-Apr., 21–24; May, 21–24). The phenomena occurring during the solidification of metals and alloys are considered. The formation of germs around which the crystals form is explained together with the control of solidification by the inoculation technique. Examples of the solidification of organic melts and their relevance to the solidification of irons are reviewed.

Review of Theories Relating to Dislocations in Crystals. J. Papier. (*Bull. Cercle Etudes Métaux*, 1954, **6**, Sept., 261–285).

The Sub-structure of Metallic Crystals. H. Nishimura, Y. Murakami, and J. Takamura. (*Kyoto Univ., Eng. Res. Inst. Tech. Rep.*, 1951, **1**, (2), Mar., 19–27). A report is made of an investigation by optical microscope, electron microscope, and X-ray methods into the grain boundary, sub-grain boundary, and mosaic block structure of Al, Al alloy, and Cu.—K. E. J.

Studies on Strain-free Cutting of Metal Single Crystals. M. Yamamoto and J. Watanabe. (*Nippon Kinzoku Gakkai-Si*, 1955, **19**, Jan., 38–42). [In Japanese]. A cutter utilizing vinyl chloride or Saran string produces practically strain-free surfaces on Sn, Zn, Fe, Cu, Ni, Al, and Bi crystals.—K. E. J.

Strength of Small Metal Specimens. C. Herring. (*Bell Lab. Record*, 1955, **33**, Aug., 285–289). The dislocation theory and its experimental verification is discussed in relation to the discovery of very small perfect crystals in the maintenance of telephone equipment.—B. G. B.

Oriented Overgrowths of Iron by Electrodeposition. J. A. James. (*Trans. Faraday Soc.*, 1955, **51**, June, 833–835). Oriented overgrowths of iron have been produced on substrates of copper, gold, and nickel–iron alloy. Oriented growth was found possible to a relatively large thickness (0·004 in.) even when there is a large atomic misfit between deposit and substrata.—B. G. B.

A Single-Profile Crystal Extensometer Adjustable for Orientation. A. J. Kennedy. (*J. Sci. Instruments*, 1955, **32**, May, 183–185). A profile-type extensometer is described for the creep testing of single metal crystals of fixed initial length. A single profile is used, set for crystal orientations of 0–50° by simple rotation. A stress variation of less than 1·5% is obtained in this range, over an extension of 100%.—L. D. H.

Transformation and Precipitation in Austenitic Chromium–Nickel Steels at Elevated Temperatures. E. Baerlecken and W. Hirsch. (*Stahl u. Eisen*, 1955, **75**, May 5, 570–579). The authors report on investigations on the effect of C, Si, Mn, Mo, Nb, N_2, Ti, and V of a number of Cr–Ni steels. The steels were heated for periods up to 18,500 h in the range 600–850° C, and it was found that the free carbon precipitates as chromium carbide after quenching. Heavy carbide contents led to embrittlement. High Mn contents cause an additional decrease in notch-impact toughness. All alloying elements which narrow the γ field promote σ-phase formation. A clear relation was found between structural changes and intercrystalline corrosion. After prolonged heating, corrosion resistance improved independently of the presence of σ.—T. G.

Temperature Hysteresis of Pearlite Conversion into Austenite under Electric Heating. A. G. Spektor. (*Met. u. Giesserei Techn.*, 1951, **1**, Nov., 345–347; *Zhur. Tekhn. Fiz.*, 1950, **20**, (9), 1136–1140). At a heating velocity up to 9% the hysteresis was found to be proportional to the cube root of the heating velocity.—L. J. L.

A Metallographic Study of the Eutectoid Transformation in Unalloyed Cast Irons. M. Ferry and G. Aubrion. (*Fonderie*, 1955, May, 4497–4512). The fundamental aspects of the eutectoid transformation in cast irons are discussed and a detailed account is given of the metallographical features of the transformation on heating and on cooling in five different cast irons. Among the irons studied were a pearlite iron, a phosphoric iron, and a malleable iron. In the neighbourhood of the phosphorus eutectic the graphitization of pearlite, the formation of austenite on heating, and the transformation of the austenite on cooling were all retarded. The form and distribution of the graphite had a considerable effect on the final structure formed on cooling.—B. C. W.

The Order–Disorder Transformation of the Alloys of Iron and Cobalt. H. Masumoto, H. Saitô, and M. Shinozaki. (*Sci. Rep. Res. Inst. Tôhoku Univ.*, 1954, A, **6**, Dec., 523–528). From a measurement of the specific heat of 17 alloys in the range of a solid solution of iron and cobalt, it was concluded that superstructures of Fe_3Co, FeCo, and $FeCo_3$ exist in the concentration ranges 20–29%, 34·6–66%, and 66–78% Co, respectively.—J. G. W.

Effect of Plastic Deformation on the Kinetics of the Martensite Formation in a High Chromium Steel. S. C. Das Gupta and S. S. Pani. (*Trans. Indian Inst. Met.*, 1953, **7**, 161–171). The effect of varying degrees of plastic deformation on the austenite–martensite transformation in a 15% Cr, 0·7% C steel has been studied in the sub-zero temperature range − 60° to − 183° C. Plastic deformation varied between 0·5%

and 20% in compression and 1% and 6% in tension. Deformation produced by either method first increased and then decreased the amounts of martensite formed subsequently by either sub-zero cooling or isothermal holding at sub-zero temperature. With smaller degrees of deformation disappearance of the mechanical stabilization effect was evidenced when held isothermally at −183° C for a long time.—D. H.

Investigation of the System Ti–Fe–Al Alloys. I. Studies on Ti–Fe System. H. Nishimura and K. Kamei. (*Bull. Eng. Res. Inst. Kyoto Univ.*, 1954, **6**, July, 38–42). [In Japanese]. Binary alloys containing 40–75% of Fe were examined. Specimens were arc-melted in an argon atmosphere and their m.p. measured optically; microscopical examinations were also made.—K. E. J.

A Supplement to Investigation of Equilibrium Diagram of Fe–As–C System. H. Sawamura and T. Mori. (*Mem. Fac. Eng. Kyoto Univ.*, 1954, **16**, July 182–189). Magnetic analyses were made on 35 As-containing irons and steels. In Fe–As alloys, the A_2 point is constant at 768° C for As < 4%, then falls and stabilizes at 730° C for As > 10%; the solubility limit of As in ferrite is approx. 10% at 730° C; Fe_2As and FeAs appear to be paramagnetic. In Fe–As–C ternary alloys the A_2 point falls as As increases; it is constant at 730° C when the phases ferrite and Fe_2As, or ferrite, Fe_2As, and cementite, are in equilibrium.—K. E. J.

Bimetals. W. Rienäcker. (*Z. Metallkunde*, 1955, **46**, June, 429–434). The general properties of thermo-bimetals are described, and the use of bimetals with alloy components is discussed, particularly alloys of the Ni–Fe series.—L. D. H.

The Diffusion Coefficient of Aluminium in Iron in the Solid-Solution Region. P. Gröbner. (*Hutnické Listy*, 1955, **10**, (4), 200–202). [In Czech]. In the range 950–1100° C the coefficient was found to be given by $D = 2 \cdot 6.10^6 \exp(-56{,}000/RT)$ cm²/day.—P. F.

The Lattice Expansion of Iron. Z. S. Basinski, W. Hume-Rothery, and A. L. Sutton. (*Proc. Roy. Soc.*, 1955, **A**, **229**, May 24, 459–467). Lattice spacings for pure Fe have been measured between 20 and 1502° C, completing data between 0° K and the m.p. The thermal expansion of the lattice and the bulk metal (where known accurately) agree well. No discontinuity in lattice spacing occurs at the A_2 point, and there is no observable difference in the coefficient of expansion of the lattice immediately above and below; the forces responsible for ferromagnetism probably do not affect interatomic distances by $> 10^{-4}$. The percentage increase in interatomic distance at the A_3 point is slightly less than the decrease at the A_4 point, and the changes agree with values calculated for the change from face- to body-centred cubic structure by the Pauling equation.—K. E. J.

A Theory of the Tensile and Compressive Textures of Face-Centered Cubic Metals. J. F. W. Bishop. (*J. Mech. Phys. Solids*, 1955, **3**, Jan., 130–142). A theory is presented, based on the assumption that each grain in the aggregate undergoes the macroscopic strain. Different textures observed under similar straining conditions by metals of the same lattice type are explained in terms of the effect of differences in relative hardening of active and latent gliding systems.—J. G. W.

Metallographic Investigation in the Faculty of Physical-Mathematical Sciences of the Eva Perón National University. (*Ing. e Indust.*, 1954, **22**, Dec., 103–104). [In Spanish]. The equipment and usage of the metallographic and heat-treatment laboratories are described. Present investigations include studies of the heat-treatment of grey cast-irons, the welding of wear-resistant materials, and the heat-treatment of aluminium bronzes.—P. S.

The Fe–Cr Superlattice. M. Tagaya, S. Nenno, and Z. Nishiyama. (*Nippon Kinzoku Gakkai-Si*, 1951, **B**, **15**, June, 235–236). [In Japanese]. The Fe–Cr alloy of composition 1 : 1 was quenched from 1000° C, annealed at 500° C for 1 week, and F.C. X-ray examination gave no indication of superlattice formation.—K. E. J.

Study on the Superlattices of Ternary Alloys by X-rays. S. Ogawa and Y. Matsuzaki. (*Nippon Kinzoku Gakkai-Si*, 1951, **B**, **15**, June, 242–244). [In Japanese]. X-ray studies on the Fe–Al–Si system (which includes the magnetic alloy 'Sendust') confirmed a composition range in which the superlattice of the 3 : 1 type can exist.—K. E. J.

Metallographic Terminology: Oligocomponents and Oligoconstituents. A. Portevin. (*Génie Civil*, 1955, **132**, Jan., 1, 13–14). The term 'oligocomponent' is recommended for elements present in very small quantities when expressed in chemical analyses. On the other hand, if the mineralogical or microscopic constituents are being described, the minor constituents should be termed 'oligoconstituents,' irrespective of whether they be present as impurities or as specific additions. Thus, in steel, the elements Al, B, N, O, P, and Cu would be oligocomponents and inclusions, oxides, sulphides, etc., oligoconstituents.—G. E. D.

Surface Micro-interferometry. J. W. Perry. (*Research*, 1955, **8**, July, 255–261). The evolution of surface interferometers is surveyed. Those depending on double-beam interference are classified as to whether the paths of the beams are common or split. A new micro-instrument of the latter type is described: applications include the examination of metallic or replica surfaces.—K. E. J.

A Low-Temperature Microscope Stage for Metal Specimens. D. Hull and R. D. Garwood. (*J. Sci. Instruments*, 1955, **32**, June, 232–233). A modification of Greninger and Mevoradian's stage, suitable for low-temperature work at high magnifications, is described. The specimen is cooled by being held in a slot in a copper rod which projects through a seal into a cooling chamber. The space between objective and specimen is surrounded by a flexible rubber envelope, through which dry air is passed. The specimen temperature is measured by a fine thermocouple held against the specimen.—L. D. H.

Improvements in Replica Techniques for the Electron Microscope. A. Fukami. (*Métaux–Corrosion–Indust.*, 1955, **30**, May, 220–228). A number of different techniques are considered, particular attention being paid to the quality of replica which can be obtained.—B. G. B.

Electron Microscopic Investigations on Metal Oxide Layers. G. Pfefferkorn. (*Z. Metallkunde*, 1955, **46**, Mar., 204–207). Investigations with the electron microscope on oxide films show the formation in many cases of needles and platelets normally to the surface. In the case of iron many of the needles take the form of broad platelets, in the form of single crystals, in the temperature range 400–600° C. At 450° C lamellæ were formed which were not single crystals, perhaps owing to the existence of different oxides. Growths of the needles at their points are explained as surface diffusion.

Structure Investigation of Multi-Component Systems by Means of Kinematic Electron Diffraction. A. Boettcher, G. Haase, and R. Thun. (*Z. Metallkunde*, 1955, **46**, May, 386–400). A method is described for the production of electron-diffraction diagrams, which include the interference patterns of the phases occurring in a multi-component system in a clearly distinguishable form, and from which the phase diagram of such a system can be constructed. The specimen is prepared by evaporating the components in successive thin wedge-shaped layers, and tempering so as to allow concentration equalization between the layers by diffusion, whilst concentration displacement along the wedge is proportionally negligible.—L. D. H.

Metallographic Application of Two-Wave Interferential Methods. G. Nomarski and Mme. A. R. Weill. (*Rev. Mét.*, 1955, **52**, Feb., 121–134). Two-wave interferometry in polarized light is very applicable to the measurement of small variations in level upon optically flat surfaces. As examples, the authors describe its use to study the growth spirals of silicon carbide monocrystals and to analyse the slips, twins and grain boundaries of samples of electrolytically polished brass, which have been lightly deformed and unattacked.

An Interferometric Study of the Action of Electro-polishing. F. Muñoz del Corral, S. Feliu Matas, and M. Serra Ribera. (*Inst. Hierro Acero*, 1955, **8**, Jan.-Mar., 62–65). [In Spanish]. The process of electropolishing the surface of 18/8 stainless steel was studied by interferometry. A series of interferograms are given together with the corresponding surface profiles. It is shown that irregularities of sizes smaller than $0 \cdot 3\mu$ are eliminated quickly but a longer time is necessary to reduce larger ones (some still remain in a bright-polished surface) and that about 3μ of metal is removed from the surface.—P. S.

An Improved Method for the Taper Sectioning of Metallographic Specimens. L. E. Samuels. (*Metallurgia*, 1955, **55**, Mar., 161–162). A simple machining jib for taper sectioning of standard metallographic mounts is described. A reference wire is buried in the protective electrodeposit on the specimen surface and is sectioned with the specimen. The major dimensions of the sectioned wire are subsequently measured and enable the taper ratio of the section to be determined.

Reactions in the Solid State Between Metal and Inclusions.

R. Zoja. (*Met. Ital.*, 1955, **47**, Jan., 15–18). [In Italian]. The author describes a particularly clear example of reactions taking place between metal and inclusions in the solid state. He shows that if inclusions are very close together during their process of reduction, the metallic matrix must be impoverished in silicon. A difference in hardness is established between the metal in the immediate vicinity of the inclusions and the metal some distance away.—M. D. J. B.

Contribution to the Study of Silicate Inclusions in Mild Steels. R. Collée. (*Rev. Univ. Min.*, 1955, 9th series **11**, Apr., 151–156). The presence or absence of silicate inclusions in irons and steels manufactured by the Thomas process is discussed. A metallographic technique for the examination of inclusions which was multiple etching with different reagents is described. This technique enables the type of silicate inclusions to be determined and examples of its application are given and discussed.—B. G. B.

The Structure of Primary Grains of Alloy Steels During High Temperature Heating. V. N. Svechnikov and B. A. Movchan. (*Zhur. Tekhn. Fiz.*, 1954, **24**, (10), 1823–1829). [In Russian]. The grain and boundary zone structures of alloy steels at high temperatures (1300–1400° C) were studied using microradiographic, metallographic, and spectral-analysis methods. Experimental results are discussed.—V. G.

The Structure of Layers of Sintered Carbides Welded on by an Electrosparking Method. E. Bryjak and W. Missol. (*Hutnik*, 1955, **22**, (3), 77–86). [In Polish]. Metallographic investigations indicated that there is no similarity in the structure of welded-on layers and that of sintered carbides. The grain size of welded-on carbides was below $0{\cdot}2\mu$. X-ray analysis showed that there is a decrease in W_2C content, the latter being formed from WC and from solid solution of WC and TiC.—V. G.

Consideration of the Recrystallizing Speed of Austenite Grains after Hot Working. N. Takeda. (*Nippon Kinzoku Gakkai-Si*, 1951, **B**, **15**, Dec., 578–581). [In Japanese]. Studies on C, Cr, Cr–Mo, and high-speed steels showed that mathematical relationships could be derived between time before recrystallization and working temperature, and time and degree of deformation.—K. E. J.

Austenite Grain-growth after Hot Working. III. High-Speed Steels. N. Takeda. (*Nippon Kinzoku Gakkai-Si*, 1951, **B**, **15**, Dec., 581–585). [In Japanese]. High-speed steel specimens were worked at 1000–1200° C and the austenitic grain growth was observed microscopically; the boundaries were revealed by holding at 800° C. The relationship between degree of deformation and grain size, working temperature, and time before recrystallization were investigated, and the progress of recrystallization was followed. Successive alternating working reduced the recrystallization tendency. Coarse grains could not be recovered by heat-treatment.

Further Studies on the Orientation Determination of Cubic Metal Crystal Rods by the Right-Figure Method. M. Yamamoto and J. Watanabé. (*Sci. Rep. Res. Inst. Tôhoku Univ.*, 1955, A, **7**, Apr., 173–183). Factors influencing precision of measurement and range of orientations for which the light-figure method is applicable, have been studied. The orientation of single crystal rods of the 6% Fe–Al alloy could be determined within 1°.—J. G. W.

CORROSION

Boiler Feedwater Treatment and Oxygen Scavengers. S. R. M. Ellis. (*Chem. Process Eng.*, 1955, **36**, Mar., 79–82). The author briefly surveys methods of removing impurities from feedwater and then discusses the corrosion problems that commonly arise. The removal of dissolved gases and the use of hydrazine as an oxygen scavenger in the place of sodium sulphite are also dealt with.—L. E. W.

System-Wide Cathodic Protection. M. G. Markle. (*Amer. Gas Assoc. Monthly*, 1955, **37**, Apr., 21–22, 48–49). The author outlines the extent of the activities of the Northern Illinois Gas Co., on new construction since 1947. The engineering forces required to carry out a programme of corrosion mitigation are analysed.—L. E. W.

Tough Old Trouper. A. C. Bining. (*Steelways*, 1955, **11**, Feb., 8–10). The author traces the history of the production of wrought iron with special reference to the developments introduced by Cort, Hall, and Aston and Story.—L. E. W.

Corrosion in the Fertiliser Factory. (*Corrosion Techn.*, 1955, **2**, Apr., 121–122). Suitable protective materials and maintenance routines are discussed.—L. E. W.

Corrosion Prevention Practice. (3) Steel Plate Pickling Shop. (*Corrosion Prevention and Control*, 1955, **2**, Apr., 27–28, 32). An outline is given of the sulphuric/phosphoric acid process developed by the Shell Petroleum Co. and Whessoe Ltd.

Cathodic Protection of Marine Installations. H. M. Powell. (*Corrosion Prevention and Control*, 1955, **2**, Feb., 30–31, 62). The author deals with the cathodic protection of static marine installations such as steel jetties, piers, etc. The factors involved in their design and in the choice of power-impressed or sacrificial anode systems are discussed. Cathodic protection is most economic when supplementing a good coating.

Principles of Corrosion. W. H. J. Vernon. (*Corrosion Prevention and Control*, 1954, **1**, Nov., 533–538, 546; Dec., 591–600, 639; 1955, **2**, Jan., 19–28, 44; Feb., 21–29). The article may be divided into two main parts: (*a*) Why metals corrode, and (*b*) the prevention of corrosion. In the first, the author discusses the electrochemical series and expresses corrosion in terms of energy considerations. The corrosion processes are classified and immersed corrosion is dealt with in some detail, with particular reference to the hydrogen and oxygen evolution types. The principles and mechanisms involved in underground and atmospheric corrosion are presented, typical examples are given and the results of various experiments are analysed. Direct oxidation reactions are considered and then the distribution of corrosion and its form of occurrence are discussed. In the second part the available methods of preventing corrosion are arranged in the following groups of related topics and described in detail—(i) Modifications of procedure: (*a*) design and structure, (*b*) surface condition, (*c*) cathodic protection; (ii) modifications of environment: (*a*) de-aeration of water or neutral solution, (*b*) purification or dehumidification of air, (*c*) addition of corrosion inhibitors; (iii) modifications of metal: (*a*) purer metal, heat-treatment to control structure, (*b*) addition of alloying elements; (iv) protective coatings: (*a*) chemical or electrochemical treatment of metal surface, (*b*) organic, inorganic, and metal coatings, (*c*) temporary protectives.

Corrosion of Oil Well Casing by Earth Currents. L. De Witte and F. J. Radd. (*J. Petroleum Techn.*, 1955, **7**, Apr., 66–72). The authors show that S.P. currents form a plausible cause for many instances of external casing corrosion where the corrosion cannot be explained by metallurgical anomalies in the pipe. The salt-mud area of Kansas is given as an example and it is suggested that casing corrosion may often be mitigated by removing the open-hole spontaneous potentials before casing is set. The mathematical derivation of current profiles from the J.P. curve is presented and the limitations of the theoretical procedure are indicated.—L. E. W.

Corrosion Research Laboratories. (3). The U.S. National Bureau of Standards. J. G. Thompson. (*Corrosion Techn.*, 1955, **2**, Apr., 102–105). The researches in progress are concerned with the corrosion of metals in service underground and in marine and other environments, and with basic considerations of the corrosion of metals with and without applied stress.—L. E. W.

Modern Practice in Cathodic Protection. G. McHardy and H. M. Powell. (*Trans. Liverpool Eng. Soc.*, 1954, **75**, 13–27). After introducing the basic principles of electrochemical corrosion and cathodic protection, the author describes the methods of applying such protection and considers the design of installations. The sacrificial anode and power-impressed systems are dealt with and the criteria of protection discussed. The problems involved in the various applications of cathodic protection and the economics of the method as a whole are outlined.—L. E. W.

The Formation of Thin Films of Iron Oxide. E. J. Caule and M. Cohen. (*Canad. J. Chem.*, 1955, **33**, Feb., 298–304). The formation of thin films of oxide on iron at room temperature and at 326–370° C is studied and discussed. A possible method for measuring the free-energy change involved in oxide film formation on metal surfaces is presented, based on measurements of the e.m.f. of the dry cell metal|oxide|oxygen.—T. E. D.

Corrosion of Aircraft Structural Materials by Agricultural Chemicals. 1. Laboratory Tests with Fertilizer Compounds. T. Marshall and L. G. Neubauer. (*Corrosion*, 1955, **11**, Feb., 44–52). Exposure of a range of aircraft materials in contact with the most common fertilizers indicated that mild steel and galvanized steel were attacked most by copper sulphate,

muriate of potash, nitrolime, and ammonium sulphate. Protection by paint is recommended.—J. F. S.

Proposed Methods for Cathodic Protection of Composite Structures. M. Unz. (*Corrosion*, 1955, **11**, Feb., 40–43). Separate anode installations, a d.c. generator with blocking rectifiers, or a.c. sources with separate rectifiers are advised for cathodic protection of parallel pipes, cables, or similar buried structures. Only if the structures are alike can they share a common system: otherwise local corrosion circuits will be produced.—J. F. S.

Corrosion and Erosion–Corrosion of Some Metals and Alloys by Strong Nitric Acid. J. F. Willging, J. P. Hirth, F. H. Beck, and M. G. Fontana. (*Corrosion*, 1955, **11**, Feb., 31–39). The corrosion of stainless steels by fuming HNO_3 increases with increasing temperature, but erosion–corrosion rates decrease with increase rate of acid flow. When coupled with Al the stainless steel is protected and the aluminium attacked at temperatures from ambient to 160° F. Polarization-curve, weight-loss, and visual studies have provided an explanation of these phenomena.—J. F. S.

A New Method for Measuring Potentials of Polarized Electrodes in Soil Corrosion Cells. W. Neighbours. (*Corrosion*, 1955, **11**, Feb., 28–30). Full details are given of the circuit and construction for an electronic method of interrupting the current through a corrosion cell and of measuring the potential during the period of interruption. The cell potential is balanced against a known potential using an oscilloscope as a null indicator.—J. F. S.

Use of High Molecular Weight Corrosion Inhibitors in Petroleum Refineries. H. H. Bennett. (*Corrosion*, 1955, **11**, Feb., 19–27). Final testing of inhibitors is best conducted in the actual refinery equipment. Three inhibitors evaluated thus using inserted steel coupons and analysis for dissolved iron reduced not only corrosion but also deposit formation in tubes and pumps.—J. F. S.

Some Remarks on Stress Corrosion Testing. H. P. Godard and J. J. Harwood. (*Corrosion*, 1955, **11**, Feb., 53–58). Precautions which must be observed in any test of stress corrosion susceptibility are examined and a selection of test methods in current use is described.—J. F. S.

95% of Valve Repair Costs are Laid to Corrosion. Galvanic Currents between Bronze, Cast Iron Noted. W. G. Ellis. (*Corrosion*, 1955, **11**, Feb., 69–74). Cost of repairing corrosion damage to gate valves handling municipal water makes it preferable to substitute valves mounted with Monel or stainless steel and high-nickel cast iron for the present bronze and cast-iron ones.—J. F. S.

The Inhibitor I.F.P. 550. M. G. Hugel. (*Corrosion et Anti-Corrosion*, 1955, **3**, Jan.-Feb., 8–12). The inhibitor, developed by the Institut Français du Petrole in response to demand by the industry, is a dibenzylxanthate of glycol and comes in the insoluble inhibitor category. The properties in relation to its structure are examined, and also the conditions under which it acts as an inhibitor.—T. E. D.

Insoluble Organic Inhibitors of Acid Corrosion of Iron. R. Jenny. (*Corrosion et Anti-Corrosion*, 1955, **3**, Jan.-Feb., 2–7). Sulphide is formed on the surface of the iron in the course of acid corrosion inhibition by mercaptans. This was studied quantitatively by a photographic imprint method, to determine which types of mercaptans were most efficient as inhibitors. Iron mercaptide is formed first, and then decomposes to iron sulphide. The ease with which this decomposition occurs depends on the type of mercaptan, and determines the degree of inhibition.—T. E. D.

Volatile Rust Inhibitors. H. R. Baker. (*Indust. Eng. Chem.*, 1954, **46**, Dec., 2592–2598). A comprehensive review of the properties and applications of many organic compounds as volatile rust inhibitors, which provide a simple method of protecting metal surfaces against corrosion. In particular, the amine salts of weak inorganic acids and amine-organic acid complexes have been found effective.—G. E. D.

Corrosion. M. G. Fontana. (*Indust. Eng. Chem.*, 1955, **47**, Feb., 81A–82A). A useful nomograph or conversion scale is presented for rapidly converting corrosion rates from one unit to another. These units include mils per year, inches per year, inches per month, and milligrams per square decimetre per day.—G. E. D.

The Stress-Corrosion Cracking of Austenite Stainless Steels. Part I—Mechanism of the Process in Hot Aqueous Magnesium-Chloride Solutions. T. P. Hoar and J. G. Hines. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 124–143). [This issue].

Effects of Sulphate–Chloride Mixtures in Fuel-Ash Corrosion of Steels and High-Nickel Alloys. H. T. Shirley. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 144–153). [This issue].

Studies on Corrosion at Hydro-Electric Plants Using Acid River Water. Studies on Corrosion of Metals. I. Districts Incurring Corrosion Damage and Types of Damage. II. Corrosion of Mild Steel in Weak Acid Water. H. Endo, S. Ishihara, and Y. Sawada. (*Nippon Kinzoku Gakkai-Si*, 1951, **B**, **15**, Oct., 491–494; 494–498). [In Japanese]. At many Japanese hydro-electric plants, corrosion is caused by acid waters (pH approx. 3–5) containing H_2SO_4, HCl, or HNO_3 of volcanic origin. In quiet water, mild steels are corroded with H_2 evolution at pH $< 4{\cdot}3$; at higher pH the corrosion rate is independent of pH. In running water, corrosion rates are higher, but are independent of pH at pH < 4, owing to the influence of dissolved O_2.—K. E. J.

The Activation Potential of Iron–Chromium Alloys with Respect to Their Chemical Resistance in Sulphuric Acid. H. J. Rocha and G. Lennartz. (*Arch. Eisenhüttenwesen*, 1955, **26**, Feb., 117–123). The activation potentials of iron, chromium, and Fe–Cr alloys in H_2SO_4 were studied at room temperature as a function of the hydrogen-ion affinity. The activation potentials of iron and chromium differ by 0·9 V and depend on the pH. The activation potential of Fe–Cr alloys decreases for an increase in chromium content from 6% to 15% by 0·6 V, from 15% to 30% Cr by 0·15 V and thereafter approaches the chromium value slowly. With regard to the pH dependence, the 'chromium passivity' is reached when the Cr content of the alloy exceeds 15%.—T. G.

Feed Water Treatment for Industrial Boilers. A. G. D. Emerson. (*Steam Eng.*, 1954, **24**, Dec., 84–86; 1955, Jan., 142–144; Feb., 182–184; Mar., 213–214, 223). Factual guidance on the selection of the most efficient methods of feed-water treatment is given. Boiler-feed systems are considered and 4 flow diagrams are given; methods of adding chemicals are described.—T. E. D.

Sacrificial Anodes at Work. T. R. B. Watson. (*Modern Metals*, 1954, **10**, Nov., 82–84). The use of magnesium sacrificial anodes is discussed. The protection of industrial hot-water tanks and ships is mentioned.—T. E. D.

Iron Bacteria in Gas Holder Water. A. R. Mitchell. (*Gas World*, 1955, **141**, Mar. 26, 840–841). The causes of growth of iron bacteria in gasholder water and their effect on paint are discussed. Various remedies are suggested. Growth can best be inhibited by addition of NaOH to give pH 7·5, and the stain on the paint can be removed with a phosphoric-acid rust remover and preventer.—T. E. D.

Some Notes on Corrosion and its Protection. J. P. Renton. (*Gas World*, 1955, **141**, Jan. 22, 255–256). Corrosion, with particular reference to buried pipes, is briefly discussed, and protection by coatings, by galvanic anodes, and by applied currents is mentioned.—T. E. D.

Underground Pipeline Corrosion and Cathodic Protection. J. F. Day. (*Gas World*, 1955, **141**, Jan. 22, 250–254). The causes of corrosion, and the theory of cathodic protection are discussed. Illustrated examples are given of various systems in which galvanic corrosion will occur, and various protecting circuits are given.—T. E. D.

Cathodic Protection. R. Hammond. (*Gas Times*, 1955, **82**, Jan. 21, 81–89). The principles of cathodic protection are discussed, and some practical features are indicated. Its use would allow an increase in the use of steel in place of cast iron, with a consequent reduction of 50% in the use of raw materials.—T. E. D.

De-Aeration of Boiler Feed Water. (*Gas Times*, 1955, **82**, Jan. 7, 21–23). Dissolved gases are often overlooked in the treatment of water to prevent corrosion. O_2 and CO_2 can cause considerable trouble. De-aerator designs are briefly discussed, and the use of chemicals, e.g. Na_2SO_3, for removing gases from solution is mentioned.—T. E. D.

Galvanic Corrosion in Underground Steel. T. R. B. Watson. (*Canad. Metals*, 1955, **18**, Feb., 18–20).

Stress-Corrosion of Stainless Steels. J. J. Heger. (*Metal Progress*, 1955, **67**, Mar., 109–116). Some suggestions for preventing or minimizing stress-corrosion are offered, but it is considered that a complete explanation of the mechanism of stress-corrosion cracking must await more fundamental information.—B. G. B.

The Pitted-Tank Mystery. Marjorie R. Hyslop. (*Metal Progress*, 1955, **67**, Mar., 91–95). A step-by-step investigation

into the failure due to pitting of three stainless-steel ammonia dissociator tanks is described. The cause of the failure was finally traced to the use of soap for the detection of leaks during pressure testing. The soap contaminated the outside surface of the tanks, causing sulphur penetration and the formation of pits.—B. G. B.

Studies on the Corrosion at the Acid River Water Electric Plants. V. Mechanism of Corrosion of Iron. VI. Cathodic Protection. H. Endô, S. Ishihara, and Y. Sawada. (*Nippon Kinzoku Gakkai-Si*, 1951, **B, 15**, Dec., 607–612; 613–616). [In Japanese]. The reaction controlling the corrosion of iron in acid river water is the diffusion of hydrogen, though in stagnant water at pH $< 4{\cdot}0$ diffusion of O_2 plays a major role as time elapses. The corrosion velocity is at a maximum at pH approx. 4·3. Trials of cathodic protection schemes for mild steels and gun-metal showed that Zn was virtually ineffective and the impressed e.m.f. method required a very high protective c.d.—K. E. J.

Studies on Corrosion at Hydroelectric Plants Using Acid River Water. III. Corrosion of Cast Iron and Stainless Steel in Weak Acid Water. IV. Corrosion by Flowing Water. H. Endô, S. Ishihara, and Y. Sawada. (*Nippon Kinzoku Gakkai-Si*, 1951, **B, 15**, Nov., 558–560; 561–564). [In Japanese]. The corrosion rates of most irons and steels in running water are equal and independent of pH, since depolarization does not occur; 13% Cr steel, however, becomes passive if the water speed is high, owing to increase in oxidizing power. Results are presented of trials under controlled conditions of mild steel, stainless steel, and gun metal in H_2SO_4 solutions. Corrosion was violent at pH $< 5{\cdot}5$–$5{\cdot}0$.—K. E. J.

A Review of the Corrosion Failures of Boiler Tubes (I). K. Takehara. (*Sumitomo Metals*, 1954, **6**, Apr., 103–113). [In Japanese]. A non-metallurgical failure is quoted, characterized by internal grooving and oxidation. The purity of rimmed-steel billets had no effect on corrosion rates in acid or neutral solutions; in alkaline solutions at 100° C the rim was attacked more than the core. The most important environmental factors are NaOH, oxidizing agents, and concentrations of alkali due to local heating.—K. E. J.

Cathodic Protection Applied to a Large-Diameter Oil Pipe Line System. D. O. Griffith. (*Corrosion*, 1955, **11**, Mar., 53–56).

Proposed Standardized Laboratory Procedure for Screening Corrosion Inhibitors for Use in Oil and Gas Wells. (*Corrosion*, 1955, **11**, Mar., 57–60. NACE Technical Committee Report 55–2 of Technical Unit Committee T–1K). The need for a standard laboratory method for screening inhibitors before field testing has been met by a simple static oil and water immersion test comparing weight losses of 1-in. square steel test coupons in the absence and presence of the inhibitor.

Solution of Cathodic Protection Interference Problems. F. W. Ringer. (*Corrosion*, 1955, **11**, Mar., 45–52). A network method for solving stray current problems can be used to predict the effect on neighbouring structures of cathodic protection installations. Neglecting polarization, voltage changes due to a test current and to the proposed cathodic protection current are directly proportional. Polarization provides a safety factor. Bond resistances required and increase in rectifier current needed consequent on bonding can be calculated from the electrical network constants of the underground structure. No special reference electrode is needed to determine these. The best anode location should be determined beforehand.

Corrosion Evaluation of Ship Bulkhead and Hull Plating by Audigage Thickness Measurements. D. J. Evans. (*Corrosion*, 1955, **11**, Mar., 23–28). The theory and use of an ultrasonic thickness gauge are discussed with reference to the observation of corrosion of ship plate. A qualitative estimate of pitting is obtained in addition to the measure of average plate thickness.—J. F. S.

Why Metals Corrode. (*Machinist*, 1955, **99**, Mar. 25, 538–552). The article forms an analysis of metal corrosion and includes uniform corrosion (rust, tanish, etc.), pitting, de-zincification parting, and intergranular corrosion in alloys. Methods of prevention are discussed.—M. A. K.

The Corrosion of Steel and Its Prevention. G. Grenier. (*Echo Mines*, 1954, Dec., 829–832).

Give Corrosion a Run for Your Money. (*Steel*, 1955, **136**, Jan. 24, 74–75). The increasing applications of zirconium metal are mentioned. Of particular interest commercially is its excellent resistance to corrosion by many chemicals, and it may be fabricated into many forms and shapes. Although expensive, it can be used economically.—D. L. C. P.

ANALYSIS

The Use of Ion-Exchange Resins in the Analysis of Coal Ash. F. Ellington and L. Stanley. (*Analyst*, 1955, **80**, Apr., 313–314). A scheme is put forward for the separation of Fe, Al, Mg, Ca, Na, and K, using Amberlite resins.—T. E. D.

Review of Industrial Applications of Analysis, Control and Instrumentation. Solid and Gaseous Fuels. H. T. Darby. (*Analy. Chem.*, 1955, **27**, Apr., 660–667). Brief notes and 135 references cover the sampling, proximate and ultimate analysis, and plastic behaviour on carbonization of solid fuels. Methods for the complete analysis of gaseous fuels are also given.—T. E. D.

The Determination of Vanadium in Highly Alloyed Steels. The Methods of Analysis Committee of B.I.S.R.A. (*J. Iron Steel Inst.*, 1956, **182**, Feb., 156–159). [This issue].

BOOK NOTICES

CHARLOT, GASTON, DENISE BEZIER, and ROLLAND GAUGUIN. "*Rapid Detection of Cations.*" Authorized translation by Ralph E. Oesper. 8vo, pp. 92. Illustrated. London, 1955: Thames and Hudson. (Price 9*s.* 6*d.*).

This little book is an American translation from the French, and this probably accounts for its unusual presentation. Although the work is described as ' Rapid Detection of Cations ' it is difficult to imagine phosphate ions coming into this category, and the means used to indicate valencies, e.g. vanadium: V (IV) and V (V), although perhaps useful, is unusual in English text-books.

In some instances, too, incorrect modern chemical nomenclature is used, for instance, ' bi-sulphate ' for ' di-sulphate.' The authors state that in some cases the concentrations are given in terms of normality and in others in molarity, but surely it would have been better to standardize on one or the other.

In their introduction, the authors deal with specificity, sensitivity, and reliability, and this theme is adhered to throughout the book. Their method is to carry out preliminary group separations to give a rough indication of the cations present. A hydrochloric acid solution is then prepared, and from the bulk solution a few drops can be taken for each of the tests in turn as indicated by the prior group separation.

Precipitation methods are very seldom employed, and, although the authors stress the fact that hydrogen sulphide is not used, the effective sulphide ion is still employed by using sodium sulphide.

The apparatus required is very simple and on a semi-micro scale, with frequent use of the centrifuge and, in some cases, of ultra-violet light to show up any fluorescence.

The reagents used are well known, and interfering elements are dealt with; but unfortunately, there is such a paucity of theoretical data that the student will need to consult some modern authoritative treatise on the subject.

The authors have rightly stressed the point that, where users of the method favour other reagents for identification, it is possible to substitute these for individual elements without affecting the principle of the method. Bearing this in mind, it would be possible to suggest more specific tests for tungsten (e.g. toluene-3:4-dithiol) and copper (i.e. sodium diethyl-dithiocarbonate or 2:2 diquinolyl). Rubeanic acid is well suited to this type of spot test technique and could have been included with advantage (as also diphenylcarbazone) as a reagent for the detection of mercury.

Generally, this handbook (for it is little more) will be useful to the practising chemist and the student, although the latter, as has already been implied, will need a very good knowledge of chemistry before he can follow the procedures involved. One has the feeling that, in an endeavour to make the text as brief as possible, much useful and relevant detail has been omitted.—EDWIN GREGORY.

LOW, BEVIS BRUNEL. "*Strength of Materials.*" Second edition, 8vo, pp. vii + 320. Illustrated. London, New York, Toronto, 1955: Longmans, Green and Co. (Price 18*s.* 6*d.*).

Basically, this compact book is intended for those who are beginning their study of the strength of materials, and it proceeds to a useful level in the subject. The volume is attractively printed and well illustrated, and contains numerous exercises and worked examples.

The second edition involves the addition of pages, which are divided almost equally into two chapters, one on mechanical testing, and the other dealing very briefly with the mechanical properties of steel, cast iron, and various non-ferrous materials. Wisely, in the last connection, the author has not attempted to describe anything of the structure of metals. The new matter reads satisfactorily and makes a definite improvement to the book.

A. R. BAILEY.

KERPELY, KALMAN. "*Az Elektroacélgyártás Gyakorlata.*" [The Electric Steel Making Process]. La. 8vo, pp. 224. Illustrated. Budapest, 1955; Müszaki Könyvkiadó. (Price 33 Forint).

This textbook, which is well illustrated with diagrams and photographs of (largely German) plant, begins with the historical development of electric steelmaking and passes on to describe the design and construction of electric arc furnaces, graphite-rod resistor furnaces, and both high- and low-frequency induction furnaces. Later chapters deal with the melting process in furnaces of all these types, and with duplex and triplex processes, as well as the Perrin and other slag-mixing processes. The final chapters deal with the making of particular kinds of steel, such as austenitic manganese, stainless, electrical, and high-speed steels, and with pouring practice for quality steels. A bibliography of 41 references indicates sources which are almost wholly German or Hungarian.—J. P. S.

THE PHYSICAL SOCIETY. "*Reports on Progress in Physics.*" Vol. XVIII, (1955). Executive Editor, A. C. Strickland. La. 8vo, pp. iv + 477. Illustrated. London, 1955. The Society. (Price 50*s.*).

Volume XVIII of these Reports keeps up the high standard of production and content established by earlier ones. Though there may be little of direct interest to metallurgists—save, perhaps, "The Displacement of Atoms in Solids by Radiation," by G. H. Kinchin and R. S. Pease, which will undoubtedly interest those concerned with the utilization of nuclear energy—there is, as usual, much which will help all scientists to keep up with modern physics, whether it be the measurement of strong magnetic fields, magnetic cooling, or the lengthy survey of field theory with which the volume concludes.—J. P. S.

MATTHES, KARL P. "*Werkzeugmaschinen für Metallbearbeitung.*" Band II. "*Fertigungstechnische Grundlagen der neuzeitlichen Metallbearbeitung.*" Sma. 8vo, pp. 101. Illustrated. Berlin, 1955; Walter de Gruyter & Co. (Price D.M. 2.40).

In the first part of their publication, reviewed in the *Journal* for January, 1955, p. 103, the author dealt with the design, and the desirability of the standardization of, machine tools. In this, the second, he deals with the fundamentals of metal removal in the operations of turning, milling, drilling, honing, tapping, etc., which will be of greater interest to the practical engineer than the first part.

NEW PUBLICATIONS

AMERICAN SOCIETY FOR METALS. "*Metal Progress.*" [Special issue of *Metal Progress*, 1955, vol. 68, No. 2-A, confined to reports of 19 ASM technical committees and can be used, along with the July 16, 1954, reports to supplement the ASM Metals Handbook]. La. 8vo, pp. 208. Illustrated. Cleveland, Ohio, 1955: The Society. Price $3.00).

BRITISH STANDARDS INSTITUTION. B.S. 11: Part 1: 1955. "*Flat Bottom Railway Rails.*" Part 1. "*Specification.*" 8vo, pp. 23. Illustrated. London, 1955: The Institution. (Price 4*s.*).

DEHLINGER, ULRICH. "*Theoretische Metallkunde.*" (Reine und angewandte Metallkunde in Einzeldarstellungen, 13. Band, Herausgegeben von W. Köster.). La. 8vo, pp. v + 250. Illustrated. Berlin, Göttingen, Heidelberg, 1955: Springer-Verlag. (Price D.M. 27.–).

"*L'Emaillage de la Tôle d'Acier et de la Fonte.*" Cours de perfectionnement des industries de l'émaillerie, session de Janvier 1951, Lyon (Rhône), organisée conjointement par le Syndicat Nationale des Émailleries sur Tôle et le Syndicat Général des Fondeurs de France, avec le concours de l'Institut de Céramique Française et du Centre Technique des Industries de la Fonderie. La. 4to, pp. 131. Illustrated. Paris [1954]: Editions Techniques des Industries de la Fonderie. (Price 1850 fr., 44*s.*).

FISCHER, HELLMUTH. "*Electrolytische Abscheidung und Electrokristallisation von Metallen.*" La. 8vo, pp. xii + 717. Illustrated. Berlin, 1954: Springer-Verlag. (Price DM. 72.–).

"*Five Small Gray Iron Foundries. Case Study Data on Productivity and Factory Performance.*" Prepared for the Foreign Operations Administration, Office of Industrial Resources, by the United States Department of Labor, Bureau of Labor Statistics (BLS Report No. 85). La. 8vo, pp. iii + 188. Illustrated. Washington, 1955: Foreign Operations Administration. (Distributed in Great Britain by the British Institute of Management, price 10*s.*).

FRITZ, J. C. "*Flammspritzen von Stahl, Metallen und Kunststoffen.*" La. 8vo, pp. 152. Illustrated. Essen, 1955: W. Girardet. (Price DM. 19.80).

MALZEW, M. W. "*Röntgenographie der Metalle.* (Zerstorungsfreie Werkstoffprüfung.) 8vo, pp. 240. Illustrated. Berlin, 1955: VEB Verlag Technik. (Price DM. 23.–).

MASING, GEORG. "*Grundlagen der Metallkunde in auschaulicher Darstellung.*" Vierte, verbesserte unde erweitete Auflage. La. 8vo, pp. vi + 153. Illustrated. Berlin, Göttingen, Heidelberg, 1955: Springer-Verlag. (Price DM. 12.60).

MATTHES, KARL P. "*Werkzeugmaschinen für Metallbearbeitung.*" Band II. "*Fertigungstechnische Grundlagen der neuzeitlichen Metallbearbeitung.*" Sma. 8vo, pp. 101. Illustrated. Berlin, 1955: Walter de Gruyter and Co. (Price DM. 2.40).

NORÉN, TORE. "*Werkstoffkunde für die Lichtbogen-Schweissung von Eisen und Stahl.*" 8vo, pp. 218. Illustrated. Göteborg, 1955: Elektriska Svetsningsaktiebolaget.

SCHUMANN, HERMANN. "*Metallographie.*" 8vo, pp. 374. Illustrated. Leipzig, 1955: Fachbuchverlag Leipzig. (Price DM. 30.–).

SERENSEN, S. W. "*Höhere Gestaltfestigkeit durch Oberflächenverfestigung.*" Translated from the Russian. 8vo, pp. 245. Illustrated. Berlin, 1954: VEB Verlag Technik. (Price DM. 18.–).

SIEBEL, ERICH (Editor). "*Handbuch der Werkstoffprüfung.*" Zweiter Band: "*Die Prüfung der Metallischen Werkstoffe.*" Herausgegeben unter besonderer Mitwirkung der Staatlichen material prüfungsanstalten Deutschlands, der zuständige Forschungsanstalten der Hochschulen der Max-Planck-Gesellschaft und der Industrie, von Erich Siebel unter Mitwirkung von N. Ludwig. La. 8vo, pp. xv + 754. Illustrated. Berlin, Göttingen, Heidelberg, 1955: Springer-Verlag. (Price DM. 118.50).

VEREIN DEUTSCHER EISENHÜTENLEUTE. "*Handbuch für das Eisenhütten-Laboratorium.*" 8vo, illustrated. Band III. "*Probenahme.*" Gemeinsam herausgegeben vom Chemikerausschus des Vereins Deutscher Eisenhüttenleute u. Chemikerausschuss der Gesellschaft Deutscher Metallhütten- u. Bergleute e. V. Pp. viii + 452. (Price DM. 45.–; members of the V.D.E. DM. 36.–). Band 4. "*Schiedsanalysen.*" Herausgegeben vom Chemikerausschuss des Vereins Deutscher Eisenhüttenleute. Pp. vi + 168. (Price DM. 22.–; members of the V.D.E. DM. 19.80). Düsseldorf, 1955: Vellag Stahleisen m.b.H.

HUGH O'NEILL, M.Met., D.Sc.

HUGH O'NEILL was born at Sheffield in 1899; after one year in a T.N.T. factory and another in the Army he graduated B.Met. with honours at the University of Sheffield in 1920. For the next 14 years he was teaching metallurgy, the first twelve months at Birmingham Technical College and afterwards at the Victoria University of Manchester. From here he published papers on the hardness, deformation, and age-hardening of metals, and by the age of 30 had been awarded the degree of D.Sc. This work was rounded off by the publication in 1934 of a book entitled "*The Hardness of Metals and its Measurement*."

In 1934 he joined the Research Department of the London Midland and Scottish Railway Company at Derby and was Chief Metallurgist from 1935 to 1947. Published work during this period was largely concerned with steels for railway work, welding, and the properties and behaviour of rails. A paper on the latter subject gained a Trevithick Premium of the Institution of Civil Engineers and he was involved in the production of a booklet on "*Rail Failures*" by the Railway Executive. During the recent war he served on many committees including one attached to the Scientific Advisory Committee of the Ministry of Supply. Until recently he was an independent member of the Ferrous Metals Committee of the Inter-Services Advisory Committee.

After 14 years of industrial experience he was appointed in 1947 to the Chair of Metallurgy at University College, Swansea, and has been teaching for 23 years. His concern with matters of general education includes two years on Derby Education Committee and several years in the Newman Association, of which he is a Founder Member, Past-President, and Chairman of the Newman International Foundation. During 1945 he was a member of the British Committee for World Student Relief, and since 1930 he has been actively engaged in the work of the National Association of Prison Visitors. With a Nuffield Travelling Fellowship he visited metallurgical plants in Canada and U.S.A. during 1950.

Professor O'Neill joined The Iron and Steel Institute 38 years ago, and has twice been awarded a Carnegie Research Scholarship. Originally a Founder Fellow of the Institution of Metallurgists, he was elected President in 1952. He acted as Chairman of the Publication Committee of the Institute of Metals from 1949 to 1953, served on the Council from 1946 to 1950, and was again elected to the Council in 1955; he was a Vice-President from 1950 to 1953. As a member for many years of the Research Board of the British Welding Research Association, he was concerned with welding work and the initiation of the Abington Research Station. Provincial activities have included Presidency of the Manchester Metallurgical Society, the East Midlands Branch of the Institute of Welding, and the Swansea and District Metallurgical Society. For the D.S.I.R. he has been a Visitor to the British Cast Iron Research Association, and has just concluded a period of service on the Council of the British Iron and Steel Research Association.

During his period of office as President of the Swansea and District Metallurgical Society (1954–1955), he served as an Honorary Member of Council of The Iron and Steel Institute.

Hugh O'Neill, M.Met., D.Sc.

Honorary Member of Council 1954–1955